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Toughness Properties of Friction Welds in Steels

Increasing work during the deceleration stage has an important effect on impact toughness

BY S. B. DUNKERTON

ABSTRACT. Charpy impact data were developed for carbon-manganese steel friction welds produced by the three variants of friction welding: direct drive, inertia and orbital. Weld microstructures and impact toughness were compared in both the as-welded and heat-treated conditions. Orbital friction welds exhibited improved impact properties.

Within direct drive friction welding, the effect of welding variables on microstructural features and toughness was studied. The main variables controlling these two properties were found to be metal displacement rate and the deceleration stage. A comparison of solid bar and tubular welds indicated better weld toughness for tubular components.

Room temperature impact toughness was generally low for direct drive and inertia welds in the 0.2% carbon, 1.5%

manganese steel, having 0.024% sulphur. However, similar steels with lower sulphur contents exhibited significantly improved toughness to the extent that base material impact properties were approached. Cleavage resistance was consistently increased by the use of postweld heat treatment, but high inclusion levels could prevent improved Charpy energies because of low tearing resistance through reoriented inclusions.

Introduction

An examination of the available literature on friction welding reveals the concentration of data on either tensile or bend testing, with little attention given to impact properties (Ref. 1). Also, even less emphasis seems to have been placed on material composition, despite the general knowledge that, for example, inclusions can impair impact behavior. This, combined with the present usage and interest being placed in friction welding for such applications as drill pipe and line pipe, prompted a program of work to analyze those factors affecting the toughness properties of friction welds in steels.

The two most widely used friction welding techniques are direct drive and inertia welding. These differ in the method by which energy is supplied (Fig. 1), the former employing a direct drive route using electric or hydraulic motors, while inertia is a stored energy technique generally using a system of flywheels. Both processes, however, utilize rotation to provide relative motion between the components. Orbital friction welding, on the other hand, uses an orbiting motion with the advantage that non-round components can also be joined (Ref. 2). The presently used system of orbital friction welding involves the rotation of both components at a common speed in the same direction with a small off-axis displacement to provide the relative motion. Deceleration is induced by the return of the off-axis component into axial alignment with its mating component. Another advantage of this third technique is the uniform velocity across the contacting surfaces, thereby avoiding temperature gradients. Schematic weld records for each of the three processes are shown in Fig. 2. These define the welding variables and highlight the simi-

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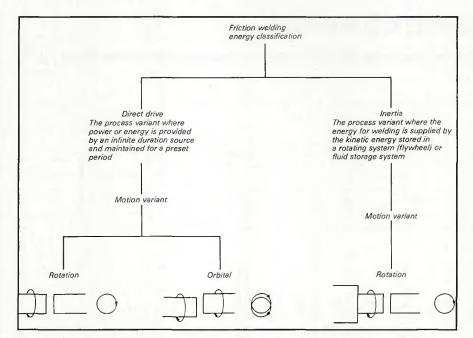


Fig. 1-Friction welding process characterization

larities and differences between the techniques. Within each process the components are brought into contact under an axially applied load (welding or friction force), at which time heating of the faying surface occurs. The metal becomes plastic and is extruded outwards to form an upset collar or flash. With inertia welding,

deceleration occurs by the loss of flywheel energy to produce the joint, with the welding force maintained for joint consolidation. Both direct drive rotary and orbital welding proceed for a predetermined weld time, set in this case by metal displacement (burnoff), at which time deceleration is activated and the

Table 1—Chemical Analyses of Steels Used in Research Program (weight percent)

			Steels		
Element	А	В	- C	D	E
С	0.21	0.19	0.24	0.15	0.26
S	0.024	0.015	< 0.005	< 0.005	< 0.005
Р	0.017	0.014	0.012	0.008	0.008
Si	0.29	0.28	0.25	0.51	0.29
Mn	1.50	1.47	1.36	1.46	0.84
Ni	0.06	0.10	0.07	0.44	0.15
Cr	0.09	0.07	0.11	0.17	0.13
Mo	0.01	0.03	0.02	0.05	0.02
V	< 0.01	< 0.002	< 0.01	0.01	< 0.01
Cu	0.10	0.17	0.07	0.11	0.02
Al	0.045	0.031	0.026	0.026	0.015

Table 2—Typical Welding Conditions Used for Comparison of the Three Friction Welding Techniques

	Continuous Drive Friction Welds	Inertia Friction Welds	Orbital Friction Welds
Rotation speed, rpm	750	3000	3000
Friction force, kN	40	91	12.4
Forge force, kN	90	91	27.8
Burnoff displacement,			
mm	5.0	5.3	2.9
Braking effort	maximum	_	maximum
Flywheel inertia,			
kg/m ²	_	0.80	_
Orbital displacement	_	-	2.6

weld is again consolidated by maintaining or increasing the axial load (forge force). During the steady state heating stage for direct drive friction welding, metal is extruded at a relatively constant rate, known as the burnoff rate.

To allow better evaluation of the three systems, data are required on the weld properties obtained. This was one objective of the present investigation, concentrating on one carbon-manganese steel to provide a direct comparison of the three techniques.

Within any of the above processes, a wide range of welding variables can be applied, each having some effect on weld metallurgy, and hence mechanical properties. One process was therefore selected for a more detailed study of welding variables. The choice of process depended on three main factors: machine availability, maximum number of possible variables, and present usage of the technique. Direct drive friction welding satisfied the requirements of having more variable possibilities than inertia welding and being more widely used than both inertia and orbital friction welding. This study initially covered 25-mm (1-in.)-diameter steel bar, but was expanded to also include 50.8-mm (2in.)-diameter steel bar and tube. The latter allowed an evaluation of conditions when moving from small to larger diameter components.

Besides the basic carbon-manganese steel used for the above studies, a plain carbon and alternative carbon-manganese steel were evaluated. Also, the steels welded covered a range of sulphur contents, thereby providing data on the effect of carbon, manganese and inclusion content on the weld impact properties.

Throughout the work, both as-welded and postweld heat treated properties were determined.

Materials and Welding Procedure

Comparison of Processes

Direct drive, inertia and orbital friction welds were produced in 25-mm (1-in.)-diameter carbon-manganese steel. The steel conformed to British Standard Specification BS2772 150M19, having a basic analysis of 0.2% C and 1.5% Mn. A more detailed analysis of this steel, designated A, is shown in Table 1.

The end of the steel components were machined flat in a lathe and given a surface finish of nominally 3 μ m. Typical welding conditions for each welding process are given in Table 2. Limited trials were undertaken on the orbital and inertia processes. Therefore, conditions were established (the latter following guidance from a United Kingdom user of inertia friction welding) based only on the

achievement of defect-free welds with high-tensile efficiency.

Selected welds were subjected to heat treatment prior to testing, the heat treatments being a normalize (850°C (1562°F) for 60 min, air cool) and a quench and temper (850°C for 60 min, water quench, 600°C (1112°F) for 60 min, air cool).

Variables Study

This study utilized Steel A, referred to above, together with Steel B, and a C-Mn steel to the same specification as A but containing a lower sulphur content of 0.015% - Table 1. The main welding variables of rotation speed, friction force, forge force, burnoff (metal displacement, which then controls weld time) and brake were examined to determine their effect on weld properties. Also, because of the importance of deceleration characteristics on weld properties, another external variable was introduced. This involved the attachment of a twisting arm to the stationary component side of the welding machine. The attachment was linked up so that actuation took place on completion of the burnoff. Because of this addition, it was necessary to utilize maximum braking of the rotating spindle to ensure that the spindle had come to rest before twisting took place.

50-mm Diameter Components

Solid bar and tubular welds were produced using the direct drive friction process. The steel used was again a carbon-manganese steel conforming to BS2772 150M19, but was of a larger diameter—50.8 mm (2 in.). Analysis of this material, Steel C, can be found in Table 1. The welding variables were extrapolated from the earlier work on the 25-mm-diameter bar. Tubular components were machined from solid bar to give a 12.7-mm (½-in.) wall thickness. A limited survey of variables was conducted on these welds to confirm the findings of the earlier study on 25-mm bar.

Postweld heat treatment was again investigated. Two treatments were adopted: normalize (850°C for 90 min, air cool) and quench and temper (850°C for 90 min, water quench, 600°C for 90 min, air cool).

Material Composition

In addition to the aforementioned welds, direct drive friction welds were produced in two further steels, listed as D and E in Table 1. Steel D is a carbon-manganese steel, but with a lower carbon content than A, B or C. The steel conforms to the British Specification BS4360, Grade 50D. The fifth steel, E, is a plain carbon steel from the Japanese specification IIS G3103, Class SB49. Both of these

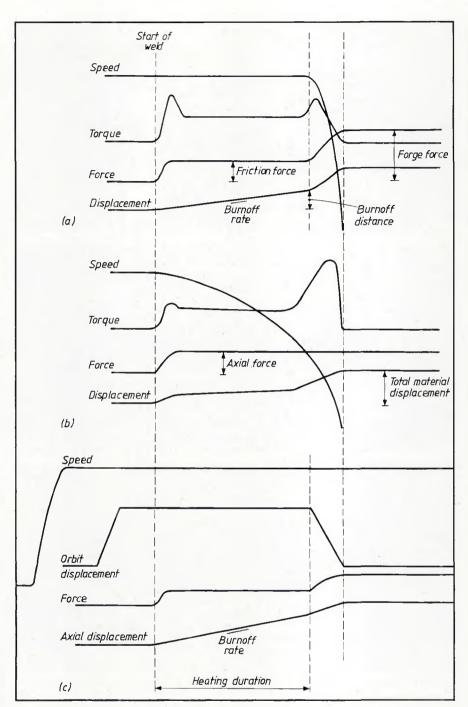


Fig. 2—Schematic of weld records for each of the three process varients. A—Direct drive; B—Inertia; C—Orbital

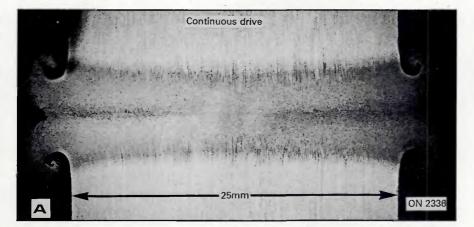
steels were plate stock from which 25-mm (1-in.)-diameter round bars were machined for the welding program.

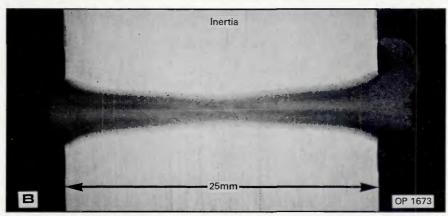
Testing Procedure

One weld from each set, *i.e.*, per material, per welding condition, per welding process, was sectioned longitudinally and prepared for optical microscopy. The polished sections were also subjected to hardness testing, using a standard Vickers hardness testing machine. Limited welds were also sectioned trans-

verse to the weld for a more detailed examination of metal flow.

Further welds were machined to provide Charpy impact specimens. The notches were always positioned on the weld center line and on the side of the Charpy specimen closest to the original weld periphery. Machining of the Charpy specimens followed the procedure whereby two specimens were obtained from each 25-mm (1-in.)-diameter weld, 8 specimens from 50-mm (2-in.) tubular welds and 9 specimens from the 50-mm-diameter solid bar welds. Testing





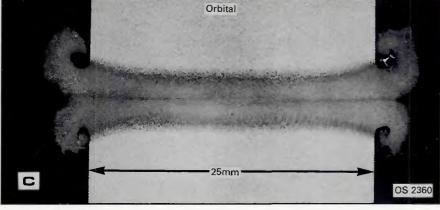


Fig. 3 – Weld macrosections in Steel A of: A – Direct drive; B – Inertia; C – Orbital friction weld •

was carried out in accordance with BS131, Part II (Ref. 3).

Scanning electron microscopy was used to analyze the failure modes of the Charpy fractures.

Results

Comparison of Processes

Typical macrosections of the welds are shown in Fig. 3. The inertia welds exhibited a distinct curved heat-affected zone (HAZ), with the minimum thickness at the weld center increasing towards the periphery. The HAZ's of the direct drive

and orbital friction welds, however, were approximately parallel sided. The amount of upset (metal flash) was dependent on the welding conditions, but for similar-sized flashes, the direct drive welds had wider weld zones than either the inertia or orbital welds.

Microstructurally, direct drive and orbital welds were similar, both containing bainitic structures, *i.e.*, grain boundary ferrite, ferrite side plates and carbide structures. In contrast, the inertia welds exhibited a very fine martensitic microstructure. Weld hardnesses were consistent with the microstructures observed. The direct drive and orbital welds had

hardnesses of 260 and 280 HV, respectively, while the inertia weld had a higher hardness of 336 HV.

Transverse sections through the weld plane were also examined to determine metal flow characteristics. In this context, the direct drive and inertia welds were similar, both exhibiting a spiral flow pattern from the center to the periphery. This was identified by the orientation of broken-up inclusions on the weld plane. The orbital friction weld, however, revealed a radial movement of metal and inclusions.

All three weld types exhibited poor impact toughness below 0°C (32°F), but above this temperature the orbital friction welds showed some improvement, while direct drive and inertia welds maintained low toughness, reaching only 25 J at 40°C (34 ft-lb at 104°F) - Fig. 4A. Postweld normalizing improved the weld toughness, and again orbital friction welds exhibited better properties, achieving an upper shelf energy of 127 J (172 ft-lb), compared to nominally 90 J (122 ft-lb) for direct drive and inertia welds - Fig. 4B.

A typical ductile Charpy failure for a direct drive friction weld is shown in Fig. 5. Microvoid coalescence dominated the failure mode, and reorientation was clearly evident. Analysis of the reoriented inclusions revealed them to be mainly manganese sulphide inclusions.

Variables Study—Direct Drive Friction Welds

From the base condition shown in Table 2, 25-mm-diameter welds were produced at higher speeds (1460 rpm), lower burnoffs (2.5 and 1.56 mm/0.06 and 0.10 in.) and reduced braking. Increasing the rotational speed resulted in a lower burnoff rate and, subsequently, longer weld time. This had the effect of increasing the heat-affected zone thickness, coarsening the weld microstructure and lowering the weld hardness. Reducing the burnoff reduced the weld time, giving a smaller metal flash and narrower heat-affected zone. The weld hardness was found to increase from 260 HV for a 5-mm (0.20-in.) burnoff distance to 337 HV for 1.5 mm. This hardness increase was associated with small amounts of martensite present in the weld microstructure. Eliminating the brake applied to the rotating chuck at the end of the heating stage caused a small increase in deceleration time which led to refinement of the weld microstructure and some reduction in weld hardness to 240 HV.

Despite these changes in weld microstructure and hardness, the Charpy impact properties of the weld remained consistently low. However, failure modes varied between the welds, the coarse

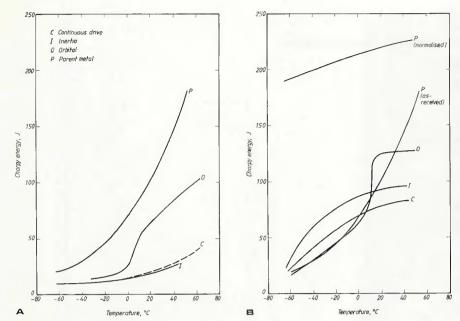


Fig. 4 – Comparison of Charpy impact energies for the three process variants. A – As-welded properties in Steel A; B – Postweld normalized properties in Steel A

microstructures giving cleavage failure while welds with finer microstructures failed partially by microvoid coalescence.

A similar exercise was repeated on 50-mm-diameter carbon-manganese steel bar (Steel C of Table 1) with similar macro- (Fig. 6) and microstructural results. However, where no variation in impact toughness was achieved with the 25-mm welds, the 50-mm welds revealed that by reducing braking (increasing deceleration time) improved impact energies from 55 to 170 J at 60°C (75 to 230 ft-lb at 140°F) were realized.

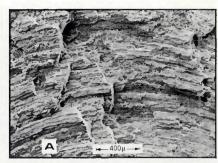
Having recognized the importance of the deceleration stage for determining weld metallurgical and mechanical properties, this phase was further investigated using a twisting device to increase the amount of work which could be introduced into the weld area. The study was conducted using Steel B, a carbon-manganese steel with a sulphur content of 0.015%. Typical friction-welding variables are given in Table 3. The twist action slightly increased the metal flash and gave a narrower heat-affected zone than comparable conventional welds - Fig. 7. Most important, however, was the significant modification of the weld microstructure from an aligned ferrite/carbide structure to an equiaxed ferrite structure containing interphase carbides. Weld hardness was reduced by the use of the twist from 275 to 237 HV. Mechanical properties such as tensile and impact were also enhanced by the use of the twist (Fig. 8), and satisfactory tensile efficiency was achieved at lower welding pressures than required if twisting was not used.

50-mm Diameter Components

The results of the solid bar welds were touched on briefly in a preceding section. The lower shelf energy levels were poor for all welding conditions examined, and although impact energies increased above 0°C, transition temperatures were generally greater than 60°C. By welding with reduced braking, however, welds were achieved with a transition temperature of about 40°C. In comparison, tubular welds, produced with nominally the same machine settings, i.e., speed and welding pressure, gave higher impact energies and reduced transition temperatures from 60° to 20°C - Fig. 9. Metallurgical examination of the tubular welds revealed narrower heat-affected zones and finer microstructures than the comparable solid bar welds. This was consistent with the fact that the tubular welds achieved higher burnoff rates than the solid welds.

Material Composition

Weld impact properties for Steels A, C, D and E are shown with their respective base metal energies in Fig. 10. All the welds exhibited poor lower shelf energies at -60° to -40°C (-70° to -40°F), but their behavior above these temperatures varied greatly. Steel A gave generally low weld energies over the temperature range -60° to 60°C. Welds in Steel B showed improvement over Steel A, with definite indications that a transition temperature was being reached at 60°C. Weld impact properties in both of these materials were substantially below those of the base material except at the lower



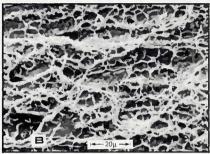


Fig. 5—Charpy impact failure for postweld normalized direct drive friction weld in Steel A. A—General view of failure; B—Detail of failure showing elongated manganese sulphide inclusion

temperature end.

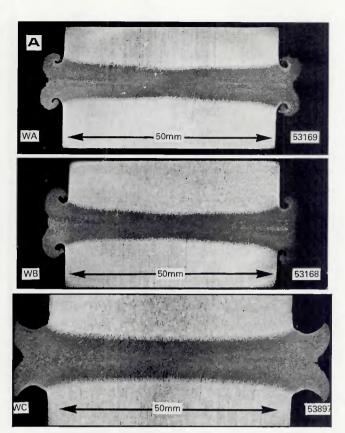
The carbon steel, E, revealed similar impact properties for base material and welds alike, while Steel D gave very high weld impact energies, reaching 180 J (244 ft-lb) at 40°C. The latter steel was obtained in the quench and tempered condition and, therefore, had high base material toughness at temperatures down to -60°C, i.e., 90 J (122 ft-lb). This low temperature toughness was lost when the steel was welded, but at the higher temperature of 40°C, the weld properties approached those of the base metal.

Fracture analysis of the weld Charpy failures revealed cleavage mechanisms for all materials at -40° to -60° C. Above these temperatures, increasing percentages of ductile failure occurred, reaching 100% for both Steels D and E. Welds in Steels A and C also became predominantly ductile as temperature increased, and elongated sulphide inclusions were observed on the fracture faces of welds in Steel A.

The weld microstructures of Steels A and C have already been described. Welds in Steel E contained grain boundary ferrite, ferrite/carbide aggregates and aligned ferrite structures, while Steel D welds consisted of a relatively finegrained ferrite/carbide structure with few aligned phases.

Postweld Heat Treatment

The effect of postweld heat treatment on impact properties is shown in Fig. 11.



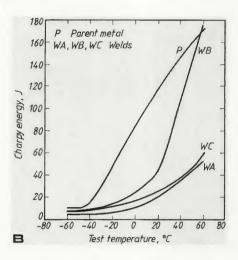


Fig. 6—The effect of welding variables on friction weld properties of 50-mm-diameter welds. WA—base condition, 360 rpm rotation speed, 78 N/mm² (5 psi) friction pressure, 186 N/mm² (10 psi) forge pressure, 3.0-mm burnoff, maximum brake. WB—same as WA but zero brake. WC—same as WA but increased speed to 700 rpm. A—Weld macrosections; B—Charpy impact properties, as-welded

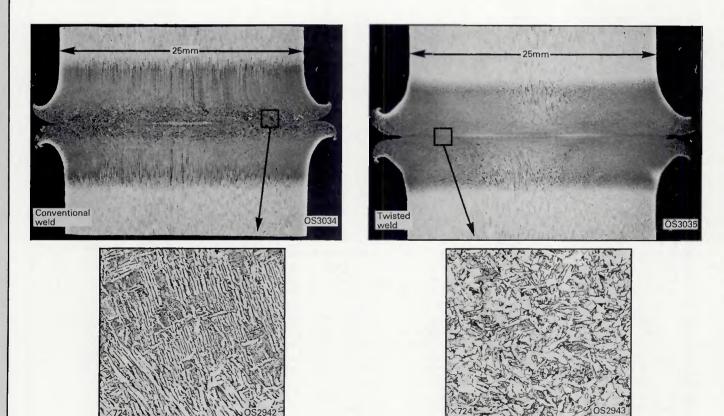


Fig. 7A – The effect of post-rotational twist in direct drive friction welding of Steel B

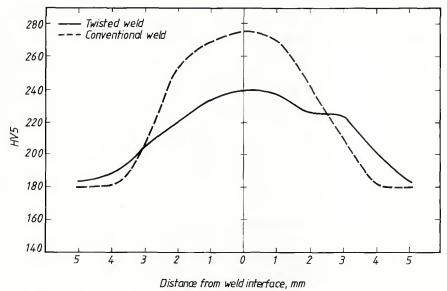


Fig. 7B—Hardness profiles of direct drive friction welds on Steel B, using a post-rotational twist

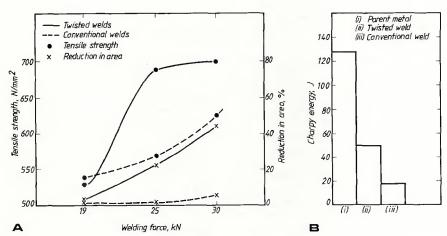


Fig. B—The effect of post-rotational twist in direct drive friction welding of Steel B. A—Tensile properties; B—Charpy impact properties at $20^{\circ}C$

Welds in Steel A had improved toughness after either a normalize or quench and temper heat treatment, but the values never reached those of the base material (compare with Fig. 10A). This behavior varies from Steel B when substantial energy increases were attained by the use of postweld heat treatment, taking the weld energies above those of the as-received base material. The heattreated weld properties were, in fact, very like those of the heat-treated base metal.

The Charpy failures of all the heat treated welds in both Steel A and Steel C occurred by ductile mechanisms. Elongated microvoids were evident on the fracture faces of Steel A, whereas the microvoid coalescence for Steel C consisted of fine spherical microvoids.

Discussion

Direct drive and inertia friction welding systems vary markedly in their energy input characteristics, resulting, as observed in this work, in quite different weld and HAZ profiles. Inertia welding provides a very rapid energy input by the use of high rotational speeds (3000 rpm) combined with the flywheel inertia. This results in rapid weld cycles, giving little time for heat dissipation along the bar; consequently, narrow and sometimes concave heat-affected zones are obtained. The versatility of the direct drive process means that conditions can be chosen to assimilate inertia welding cycles, but generally lower speeds are used, enabling the use of lower welding forces. One advantage of this lower heat

Table 3—Typical Friction Welding Conditions for Twist Investigation^(a)

	Conventional Weld	Twisted Weld
Rotation speed,	1460	1460
Friction force,	1-100	1100
kN	33	33
Forge force, kN	33	33
Burnoff, mm	1.5	1.5
Braking	maximum	maximum
Twist delay	_	0
Twist angle, deg	_	85
Twist rate,		
deg/sec		22

(a)Twist device was actuated on completion of burnoff with 0.5-s set delay to allow rotating spindle to decelerate to zero speed. Above variable of twist delay allows for additional delays to be set.

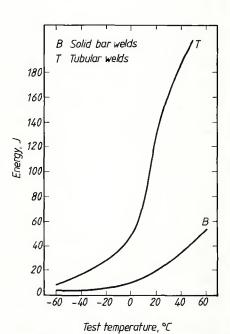


Fig. 9 – Comparison of Charpy impact properties for direct drive friction welds in bar and tube components. Welding condition set as follows: 360 rpm rotation speed, 78 N/mm² friction pressure, 186 N/mm² forge pressure, 3.0-mm burnoff, maximum brake

input, in comparison to inertia welding, was the lower weld hardness obtained -260 HV for direct drive friction welding as opposed to 336 HV for inertia welding. However, despite the microstructural differences between the two welding processes, similarly poor impact energies were obtained. From fracture analysis it was determined that both weld types failed in a predominantly brittle manner at low testing temperatures, which could be related back to the aligned ferrite constituent in the direct drive welds and to the martensite in the inertia welds. The incidence of microvoid coalescence at higher testing temperatures did not yield

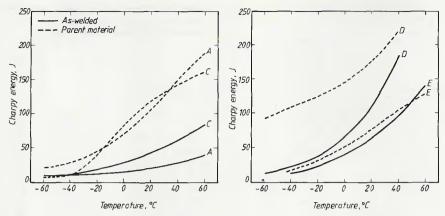


Fig. 10—Charpy impact data for base steels A, C, D and E (Table 1), and their resultant weld properties

improved impact energies because of the association of the voids with elongated manganese sulphide inclusions, giving low tearing resistance. These inclusions, for both welding processes, are present from the original bar material, but have become broken up and reoriented into a transverse direction as a result of the welding process. This phenomenon was also reported recently in another research program that examined the effect of process variables on inertia weld properties in a low-alloy steel.

The orbital friction welds were similar in appearance to the conventional direct drive rotary welds. One aspect to note, however, is the significantly reduced welding forces needed for orbital friction welding to produce welds with similar metallurgical features - Table 2. This is believed to be due to the increased heating efficiency of orbital friction welding because of the uniform velocity across the faying surfaces. Uniform velocity means uniform heating, such that welding temperatures are reached at a much faster rate than for conventional friction welds. One feature of orbital friction welding which may have given cause for concern is the peripheral area of the weld which is exposed to the atmosphere during the welding cycle. However, the region is rapidly covered by extruding metal flash and any surface oxidation is forged out into the flash collar in the final stage of welding. No evidence of interfacial oxidation was found on the weld cross-sections.

It is interesting to note that despite the similarities between orbital and direct drive friction welds, the former exhibited improved toughness above 0°C (32°F). At these temperatures, the failures occurred by a combination of cleavage and microvoid coalescence. Further analysis of both the Charpy failures and transverse cross-sections revealed a difference in inclusion orientation between the two weld types. The inclusions in orbital friction welds moved radially outwards from the weld center, while those

in direct drive (and inertia) followed a spiral (helical) pattern. This subsequently meant a difference in orientation with respect to the Charpy notch location, with improved toughness being obtained when the inclusions lay normal to the notch. It is possible that this orientation difference could be reduced or removed by using a forge delay on the direct drive friction welds, which would tend to produce a radial pattern.

The comparison between processes was basically conducted using only a limited number of welding conditions for each welding technique. However, each welding variable affects either heat input or the amount of deformation introduced to the weld, thereby affecting both metallurgical and mechanical properties. To establish an understanding of the preferred welding variables which satisfy particular joint properties, an investigation of variations was conducted using direct drive friction welding. Those variables which provided more time for heat development allowed austenite grain growth to proliferate, resulting in coarser weld microstructures. Also, heat conduction along the bar length could occur, giving wider heat-affected zones. The increase to final grain size, particularly of the ferrite side plates, led to a reduction in cleavage resistance, and hence impact properties remained low, even up to the higher test temperatures of 60°C. The coarser weld microstructures did, however, result in a reduction in weld hardness.

Typical changes in variables to give the above effect were increased burnoffs, reduced friction loads and increased rotational speeds. The latter two increase weld time by a reduction in the burnoff (metal displacement) rate. It follows, therefore, that increased burnoff rates have the advantage of increasing weld impact properties. One probable disadvantage of increasing the burnoff rate would be an increase in weld hardness.

One other variable having a pronounced effect on both microstructure

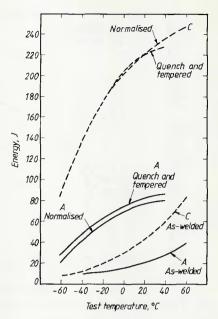


Fig. 11—Effect of postweld heat treatment on weld impact properties for Steels A and C

and impact properties was the deceleration stage (controlled by braking effort in conventional direct drive friction welding). In the present work, an increased deceleration time, i.e., reduced braking, led to finer microstructures with an increase in ferrite formation. Previous work has examined the deceleration phase in friction welding with respect to its effect on final peak torque (Ref. 4), macroscopic weld features, i.e., plasticized layer and flash profile (Ref. 5), and tensile strength (Ref. 4). It was established that increased deceleration times resulted in higher peak torques, continuous plasticized regions, larger metal flashes and improved tensile efficiencies. The results developed here take this a stage further in that the additional working of austenite during deceleration also refines the weld microstructure, and therefore has a beneficial effect on weld toughness

The addition of a twisting action after deceleration would further hot work the weld region during the critical cooling temperature range, thereby giving further improvement to the weld properties. The great advantage of modifying weld microstructure and improving impact properties during or immediately after deceleration is that both can be achieved without detriment to the weld hardness. Indeed, the degree of work introduced by twisting can be sufficient to reduce weld hardness. The twisting technique has been used previously by the Japanese (Ref. 6) to provide correct orientation of parts after rotational friction welding. It is very useful, therefore, to know that the twist does not degrade joint guality, and with the improvements actually obtained, the technique may find wider usage.

The other important feature of the twist technique is the ability to produce satisfactory tensile and bend properties at lower welding forces than required in conventional friction welding. It is therefore viable that the capabilities of existing direct drive equipment could be extended to larger section sizes. This would of course be dependent upon the power limits of the machine.

The extrapolation of welding conditions from 25-mm-diameter bar to 50mm-diameter bar was shown to be possible, and this needs to be investigated further for larger diameter components. If the successful scaling up of welding variables proves possible for diameters up to 150 mm (6 in.), the technique could be applied to on-off repair applications, as in damaged shafts and rolls. Somewhat different behavior was found between solid bar and tubular welds, with tubular welds having better impact characteristics than solid section welds for a similar set of welding conditions. This was related to the difference in burnoff rate between the two weld geometries. It was shown earlier that high burnoff rates are beneficial, and when welding tubes, the higher rates can be achieved because of the two following factors:

- Easier metal flow to both the ID and OD of the tube.
- The absence of a central, zero speed portion of weld metal which would otherwise need to be heated by conduction from other areas of the weld

Friction welding is a widely used technique for joining many steel types. The present work has examined a small number of these steels to demonstrate the difference in joint properties achieved and to show how steel selection is important prior to application development. The steels were mainly carbon-manganese steels, three conforming to Specification BS2772 150M19 (0.2% C, 1.5% Mn) and one to B54360. Grade 50D (0.15% C, 1.5% Mn). The fifth steel was a Japanese carbon steel complying to JIS G3103, Class SB49, with a carbon content of 0.26%. The variation of compositional elements between steels cast to the same specification can be sufficient to affect weld properties. Possibly the most significant element affecting weld toughness in this case is the steel sulphur content. Steels A and C (both 0.2% C, 1.5% Mn) responded differently to impact testing at temperatures above -20°C (-4°F) in the as-welded condition, and even more so in the heat-treated condition. At these temperatures, failure occurred by a combination of cleavage and microvoid coalescence. Microstructural differences were insignificant, and therefore, it can be assumed that the microvoid coalescence energy varied between the two welds. Examination of the fracture surfaces did reveal a difference in behavior, with distinct elongated voids containing manganese sulphide inclusions for Steel A (0.024% S), while Steel C (<0.005% 5) exhibited small spherical microvoids with little evidence of sulphide inclusions. The reorienting effect of the welding process is such that longitudinal inclusions in the base material are moved into a transverse orientation. Tests on the weld interface are therefore, in effect, measuring the transverse properties. Reducing the level of the inclusions in the base material will significantly improve the weld properties, particularly toughness.

Reducing the carbon content to 0.15% for carbon-manganese steels (Steel D) was found to be of benefit in reducing the joint transition temperature (compare Steels C and D, Fig. 10), although the slightly higher nickel content (0.44% compared to 0.1%) may have also contributed. The reduction in energy from the base material level of 90 J (122 ft-lb) to weld values of approximately 10 I (14 ft-lb) at -60°C was a result of the microstructural transformation. Steel D was obtained in the guench and tempered condition and therefore exhibited very good toughness, but this treatment was destroyed by the weld thermal cycle.

The carbon steel, E, is a very useful example, demonstrating that the impact resistance of friction welded joints can be the same as that of the base material. Again, a very low sulphur content was required to ensure the same toughness levels were achieved. In comparison to the other materials, the carbon steel gave better weld toughness than the two 0.2% C, 1.5% Mn steels (A and C), possibly indicating that a lower manganese content is of benefit (higher carbon generally reduces impact toughness).

Lower temperature (-60° to 0°C/ -76° to 32°F) impact properties were generally improved by either a normalize or guench and temper heat treatment. This was combined with a lowering of the transition temperature, and both were achieved by the development of tougher microstructures, either equiaxed ferrite and pearlite or a tempered bainitic/martensitic structure. By increasing the cleavage resistance, microvoid coalescence was induced and, provided the inclusion content was low, impact energies were increased. It must be remembered that no amount of heat treatment after welding can improve toughness if reoriented inclusions are present. These form a plane of weakness along which cracks can readily propagate. It is difficult to specify an exact compositional limit for sulphur content based on the limited number of steels examined. Obviously, less than 0.005% is an ideal case, but less than 0.015% is probably more realistic. Alternatively, other researchers have examined the effect of calcium treatment

on the friction weldability of various steels (Ref. 7). This treatment reduces the overall sulphur content and also modifies the shape of the remaining inclusions to a spherical rather than elongated form. The conclusion of the work was that weld ductility and toughness could be significantly improved by the use of calcium treatment.

In summary, friction weld toughness is dependent on: 1) welding process; 2) welding variables, namely, heat input and deformation characteristics; and 3) material composition. With correct choice of each of these, friction weld impact properties can equal those of the base material. Where this is not possible, postweld heat treatment can be used to improve weld cleavage resistance.

Conclusions

- 1. With the possible exception of orbital friction welds, the as-welded Charpy impact properties of friction welds in a nominal 25-mm (1-in.)-diameter 0.2% C, 1.5% Mn steel specified to BS2772 150M19 were poor over the temperature range -60° to 60° C (-76° to 140° F). These poor properties are attributed to both unfavorable transformation products and the quantity of reoriented sulphide inclusions at the weld interface.
- 2. In the as-welded condition, orbital friction welds have exhibited improved impact properties above 0°C (32°F) when compared to direct drive and inertia friction welds. It is suggested that this is a function of the metal/inclusion flow being radial for orbital welds and spiral for direct drive and inertia friction welds.
- 3. With direct drive friction welding, improved impact properties were achieved by the use of higher burnoff rate conditions and slower deceleration times
- 4. In direct drive friction welding, increased working after deceleration through the use of a twisting device has modified the weld microstructure, giving lower weld hardness and improved impact resistance. Also, satisfactory weld tensile efficiency was achieved at lower welding pressures than required for conventional direct drive friction welding.
- 5. Friction welds on tubular material have exhibited better impact energies than welds on solid bar, each produced under comparable machine settings.
- 6. Direct drive friction welds in 25-mm-diameter carbon steel specified to JIS G3103 gave impact properties similar to those of the base material.
- 7. A comparison of steel types encompassing three carbon-manganese and one carbon steel indicated the benefit of reduced carbon and manganese levels as a means to improve as-welded friction weld toughness.

- 8. Steel sulphur content was a prime factor influencing weld impact properties in both the as-welded and heat-treated conditions. Low sulphur contents were required to enable good toughness to be achieved from the friction welds.
- 9. Postweld heat treatment of either a normalize or quench and temper steel improved weld impact properties, provided the steel sulphur content was low.

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Reminder to Authors

Completed Author Application Forms and accompanying 500-word abstracts of papers proposed for presentation at the 18th International Brazing and Soldering Conference must be mailed by August 15, 1986, to ensure their consideration. The Brazing and Soldering Conference will be held March 24–26, 1987, in conjunction with the 68th Annual AWS Convention in Chicago, III. Author Application Forms appear opposite the Reader Information Card in the March 1986 issue of the Welding Journal.

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Papers are being solicited for an AWS/AWI-sponsored international conference, "Applications of Electron and Laser Beam Welding," to be held in Hartford, Conn., September 16-17, 1987. The conference will cover present and potential production applications and laboratory innovations of both of these forms of energy beam technology. Specific topics will include heat treatment, thin material welding and cutting, dissimilar metal joining, and out-of-vacuum electron beam processing, among others. Application areas include shipbuilding, nuclear, transportation and consumer products. Conference proceedings will be published. Submit abstracts of 200–250 words by November 30, 1986, to Dr. H. G. Ziegenfuss, Technical Director, American Welding Society, 550 N. W. LeJeune Rd., P. O. Box 351040, Miami, FL 33135. Full papers for those abstracts selected will be required by March 1, 1987.