PRESSURE REQUIREMENTS FOR DIFFUSION BONDING TITANIUM

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The requirements of pressure-time combinations for achieving intimate contact between two surfaces to be solid-state bonded are considered. The bond surfaces are assumed to exhibit relatively rough surfaces as a result of prior machining or other surface preparation, and the resultant surface asperities are considered to be removed by creep deformation under pressure during the bonding process. The deformation of both continuous groove-like asperities and local pyramidal-like asperities are considered analytically. The analysis provides a quantitative method for assessing the complex inter-relationships among pressure, bond time, and bonding temperature. Comparison with experimental results demonstrates generally good agreement with the prediction of these parametric combinations.

Introduction

Diffusion bonding, or solid state joining, is a technique which has demonstrated promise in the joining of many similar and dissimilar metals and alloys, and is receiving increasing interest in the joining of Ti alloys. It is a process whereby metals may be joined together in an inert atmosphere and under pressure for a period of time, and at a temperature sufficiently high to allow some creep deformation, interface diffusion, recrystallization, and grain growth to take place. The mechanisms of the solid state joining process have been the subjects of numerous studies (1-7) and, although considerable insight has been achieved, the details of the interface bonding mechanisms remain for the most part ill-defined. Diffusion welding is, however, generally agreed to take place in the following basic stages: 1) development of intimate physical contact, 2) formation of the metallic bond, 3) inter-diffusion, and 4) recrystallization and/or grain growth across the interface.

The development of the physical contact probably occurs through initial removal of local asperities by interface creep (2) under applied pressure, followed by the removal of micro-pores, or voids, by a diffusion-aided mechanism (5).

Diffusional processes probably also aid the bonding in many metals such as titanium which can readily dissolve minor amounts of adsorbed gases and thin surface oxides and diffuse them away from the bonding surfaces so that they will not impede the formation of the required metallic bonding across the bond interface. The need for further atomic diffusional interchange and grain growth across the interface is controversial (3), but these effects probably occur in most bonds developed at the high temperatures normally used.

The removal of asperities by plastic deformation (i.e., creep) under pressure has been discussed by several investigators (1, 2, and 5), and some (2) concluded that in the initial stage of the diffusion welding, the creep deformation of the surface and not the diffusion coefficient will be the rate controlling mechanism. Most of the experimental studies related to deformation requirements have dealt with the measure of total deformation of components bonded rather than the more inaccessible interface deformation, and the effect of surface finish. No quantitative studies have been reported which describe the closure of bond surfaces by deformation of surface asperities under applied pressure.

An analytical method is presented herein to define the relationships among temperature, pressure, and time required to remove asperities by creep deformation and thereby achieve intimate mechanical contact across the bond line. The analysis is limited to the surface deformation aspects of solid state bonding and does not consider other equally important mechanisms such as the dissolution of adsorbed gases, diffusion-aided removal of micropores, or grain growth. The analysis is applied to and checked against the Ti-6%Al-4%V alloy for the following reasons:

1) this alloy is readily diffusion bonded to yield full strength equal to parent metal strength if properly processed.

2) considerable diffusion bonding data are available for a wide range of parameters.

3) high temperature creep deformation data are available for application of the analysis.

To illustrate the sequence of asperity deformation with this alloy, a diffusion bond was produced under a pressure gradient and examined metallographically. The pressure gradient was achieved by machining a thickness taper into one Ti-6Al-4V sheet, and bonding a second "as-rolled" sheet of uniform thickness to it. The machine surface finish was 180 RMS, and that of the "as-rolled" sheet 64 RMS. The parameters used were 1700°F, 5 hrs, and nominal 2,000 psi over the specimen plan area. Because of the thickness taper, the actual interface pressure varied from 0 psi to well over 2,000 psi. The resulting sequence of the bonding is presented in Figure 1, where the effect of the pressure gradient on the removal of asperities is clearly illustrated. It is apparent that the surface deformation of the asperities and the resulting area of contact across the bond line is directly related to the pressure applied. It is noteworthy, however, that as the interface voids become small, they become spherical in shape, indicating that surface tension and diffusional aspects are becoming effective. Also of interest is the apparent bridging which occurs under very light contact and suggests that vapor phase transport also occurs at this bonding temperature. This sequence clearly illustrates the importance of applying of sufficient pressure during the initial stages of bonding to overcome surface asperities. With insufficient pressure, complete bonding may not be achieved even though diffusion and vapor transport mechanisms are operative.

Analytical Discussion

The analytical approach presented here is one of considering the deformation of surface asperities only, such that they are creep deformed under pressure to yield a flat and intimate contact face across the bond area. Two types of asperities are considered: 1) continuous long machine grooves and 2) local pyramidal asperities. The analytical considerations for these two cases are somewhat different. Metal flow along the length of a continuous

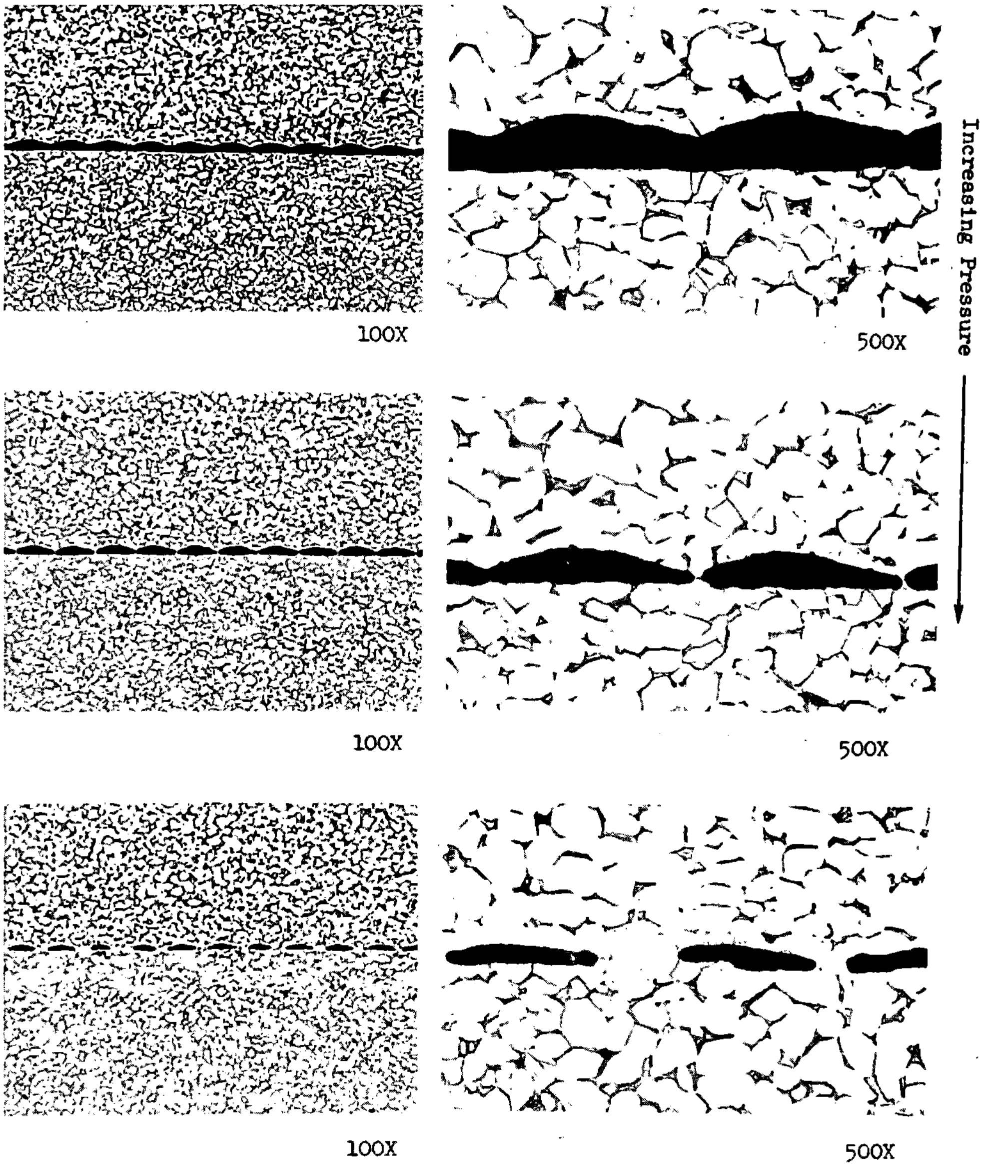


Fig. 1. Sequence of diffusion bonding in Ti-6Al-4V under a pressure gradient.

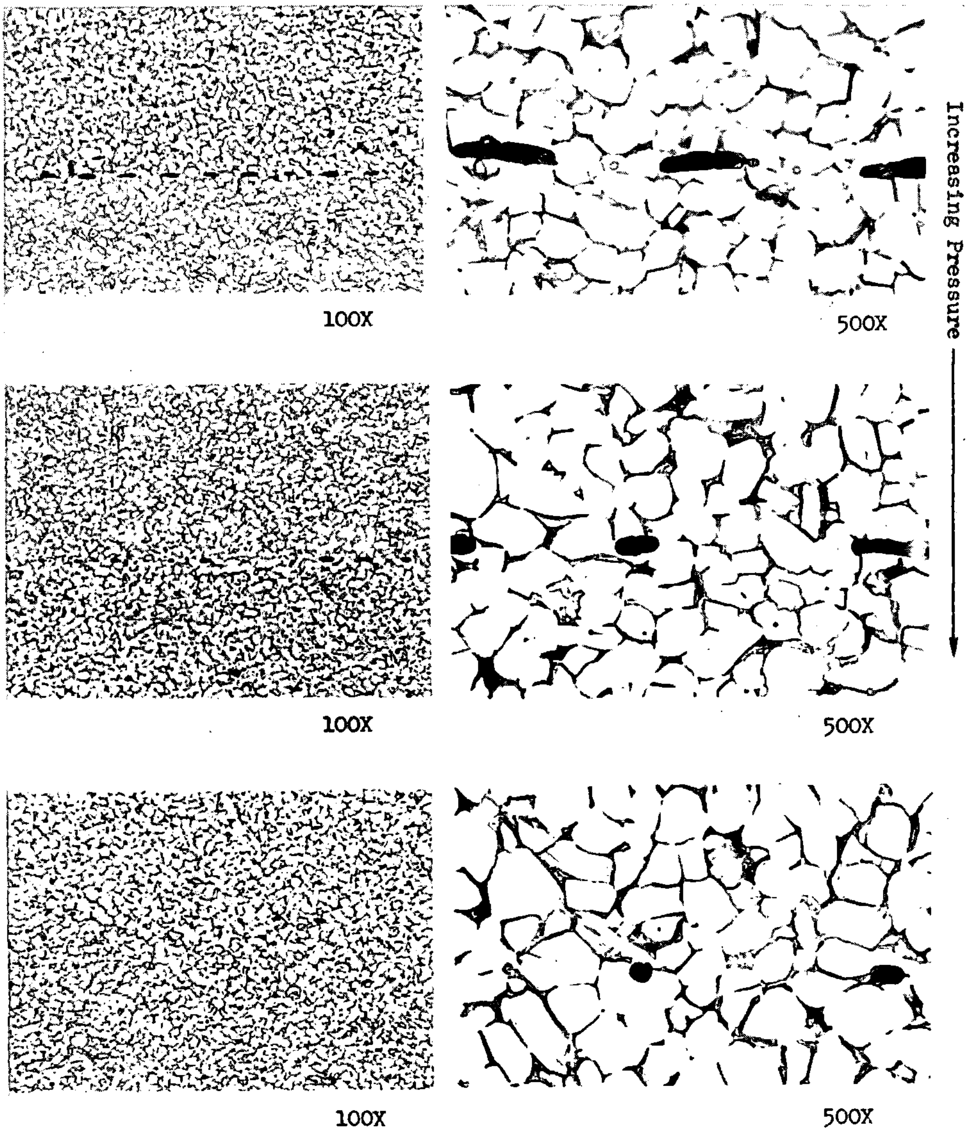


Fig. 1. (cont'd)

asperity is unlikely and therefore would create a plane strain condition, whereas local, or pyramidal-like, asperities may deform with equal ease in all lateral directions and hence a state of stress roughly equivalent to plane stress would be developed for this case.

An idealized asperity shape and coordinate system are presented in Figure 2. For the purpose of analysis, an idealized sequence of asperity deformation is assumed as presented in Figure 3 such that the asperities are triangular in cross section and meet point-to-point across the bond interface. As closure proceeds, the asperities contact on a planer intersection. For the sake of simplicity, the average values of strains and stresses required for complete contact are considered.

The average plastic strain normal to the interface, ϵ_z, required to completely remove the interface asperity is:

$$\epsilon_z = \ln \frac{d_f}{d_o} \qquad [1]$$

where d_o = initial asperity height

d_f = final asperity height (initial height minus depth to the resultant bond line)

For the asperities of Figure 3, the final asperity height, d_f, will be $d_o/2$ since the volume of displaced metal will just equal the remaining volume of the machine cavity. Thus, the strain, ϵ_z, is:

$$\epsilon_z = \ln \frac{d_o/2}{d} = \ln \frac{1}{2} \qquad [2]$$

This consideration should hold also for more realistic surface asperities which are statistically symmetric about the resulting bond plane such that $d_f = d_o/2$.

The remaining two principal strains, ϵ_x and ϵ_y, can be established from the assumption of incompressibility:

$$\epsilon_x + \epsilon_y + \epsilon_z = 0 \qquad [3]$$

and from the assumed state of strain (i.e., plane strain or plane stress). For the plane stress condition,

$$\epsilon_x = \epsilon_y \qquad [4]$$

and for plane strain,

$$\epsilon_x = 0 \qquad [5]$$

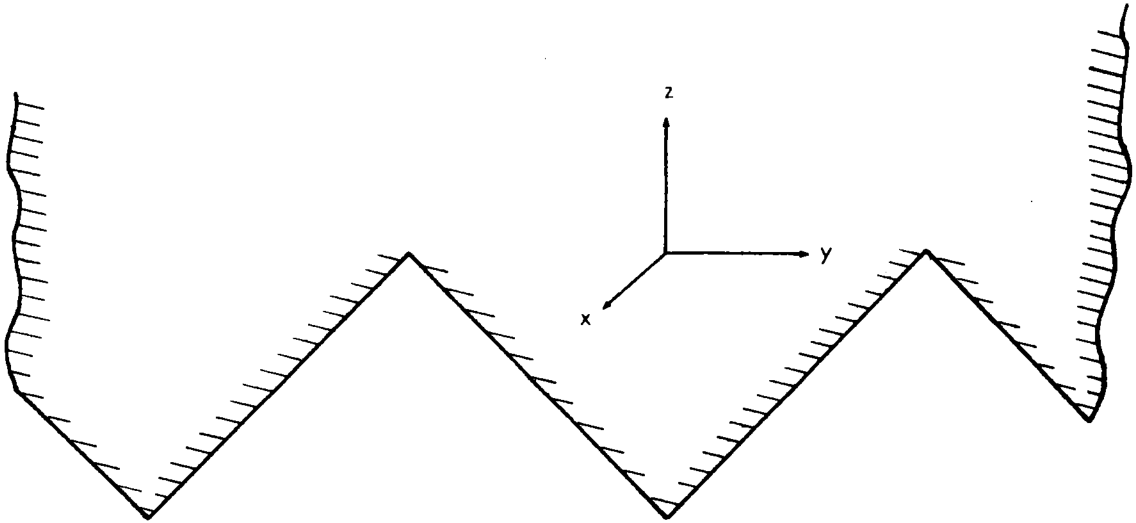


Fig. 2. Idealized asperity cross-section and coordinate system.

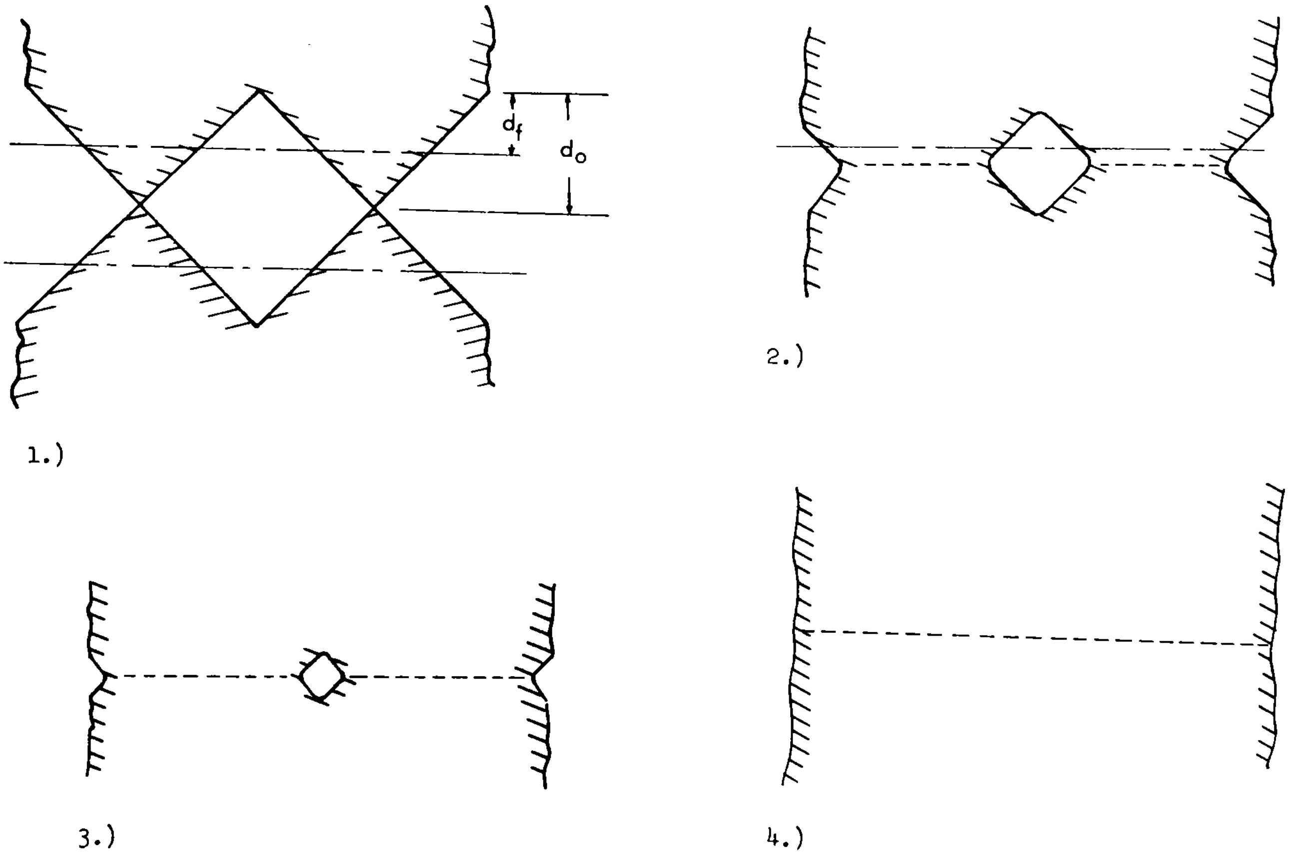


Fig. 3. Sequence of deformation of idealized asperities.

Combining equations [4] and [5] with equation [3] , the principal strains are established.

Plane stress:

$$\epsilon_x = \epsilon_y = \frac{\epsilon_z}{2} \qquad [6]$$

Plane strain:

$$\epsilon_y = -\epsilon_z \qquad [7]$$

These strains are the average total strains required to remove the asperities over the bonding time period, t_b , from pressure application to complete interface contact. Consequently, the average strain rates can be established by normalizing the average strains with the bonding time, t_b , such that

$$\begin{aligned} \dot{\epsilon}_z &= \frac{\epsilon_z}{t_b} \\ \dot{\epsilon}_x &= \frac{\epsilon_x}{t_b} \\ \dot{\epsilon}_y &= \frac{\epsilon_y}{t_b} \end{aligned} \qquad [8]$$

The average stresses in the asperities resulting from the bond pressure applied, P_b , are developed similarly. Average normal stress, σ_z , is determined by the applied load, $P_b \times A$, and the average area of the asperity, $A/2$:

$$\sigma_z = \frac{P_b \cdot A}{A/2} = 2 P_b \qquad [9]$$

where A is the total area of the specimen under consideration.

For the conditions of plane stress, the x and y components are zero because of the free surfaces:

$$\sigma_x = \sigma_y = 0 \qquad [10]$$

For plane strain, the y component will also be zero because of the free surface at the sides of the asperities, and σ_x can be determined as a function of σ_z as follows (8):

$$\begin{aligned} \sigma_x &= \frac{1}{2}(\sigma_z + \sigma_y) \\ &= \frac{\sigma_z}{2} \end{aligned} \qquad [11]$$

The relationship between multiaxial stresses and strain rates can be described through a consideration of effective stress, $\bar{\sigma}$, and effective strain rate, $\dot{\bar{\epsilon}}$, such that (9):

$$\bar{\sigma} = f(\dot{\bar{\epsilon}}) \qquad [12]$$

where

$$\sigma = \frac{1}{\sqrt{2}}\{(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2]^{1/2} \qquad [13]$$

and

$$\dot{\bar{\epsilon}} = \frac{\sqrt{3}}{2}[(\dot{\epsilon}_x - \dot{\epsilon}_y)^2 + (\dot{\epsilon}_y - \dot{\epsilon}_z)^2 + (\dot{\epsilon}_z - \dot{\epsilon}_x)^2]^{1/2} \qquad [14]$$

where the above stresses and strains are in principal directions. From equations [13], [11], and [9], the relationship between effective stress and bond pressure can be established for plane strain conditions:

$$\bar{\sigma} = \frac{\sqrt{3}}{2}|\sigma_z| = \sqrt{3}|P_b| \qquad [15]$$

Similarly, the relationship between effective stress and bond pressure for plane stress can be established from equations [13], [10], and [9]:

$$\bar{\sigma} = |\sigma_z| = 2|P_b'| \qquad [16]$$

P_b and P_b' above represent bond pressures for the plane strain and plane stress conditions respectively.

The effective strains for plane strain condition are determined by substituting equations [7] and [8] into equation [14]:

$$\dot{\bar{\epsilon}} = \frac{2}{\sqrt{3}}|\dot{\epsilon}_z| \qquad [17]$$

For plane stress, the effective strain is determined by substituting equations [6] and [8] into equation [14]:

$$\bar{\epsilon} = |\dot{\epsilon}_z| \qquad [18]$$

The required equations are now available to determine the relationship between applied bond pressure, P_b, and time to overcome asperities, t_b, provided that the required material properties relating stress ($\bar{\sigma}$) and strain-rate ($\dot{\bar{\epsilon}}$) are available (equation [12]).

To compute the pressure requirement, it is simply necessary to calculate the average effective strain rate (equation [17] or [18]), establish the corresponding flow stress ($\bar{\sigma}$) for the material and temperature under consideration (from experimentally determined data), and then complete the required bond pressure, P_b or P_b' (equation [15] or [16]).

Comparison To Data

The above development contains several assumptions and utilizes average values of stress and strain components for the sake of simplicity. To assess the value and limitations of this development, it is necessary to compare predictions based on this development to experimental results. Because of the multitude of tests conducted with Ti-6Al-4V by many investigators, these abundant tests results provide an excellent source of data for a preliminary check of the analytical model. These test data were utilized where bonding parameters were reported, and where resulting bond strengths as well as parent metal strengths were determined.

For comparison, the reported bonding parameters of time and temperature were considered in the analysis, and the predicted pressure, P_b , was computed. The high temperature stress/strain-rate relations employed were taken from two sources (10 and 11), and are presented in graphical form in Figure 4 as a log σ vs. log $\dot{\epsilon}$ for several temperatures, and provide the data necessary for relating $\bar{\sigma}$ and $\dot{\bar{\epsilon}}$ of equation [12] . For reported bonding temperatures other than those presented in Figure 4, the data were approximated by interpolation. The resulting bond pressures predicted by the analysis were compared directly to the pressure actually used, and the resulting diffusion bond strength compared to parent metal strength provides a gage of the extent of bonding. This comparative analysis was conducted for both plane stress and plane strain conditions. Bonding temperatures reported varied from 1350°F to 1750°F, and bond times from 5 minutes to 16 hours, and pressure from 25 psi to 5000 psi.

The results are presented by comparing the ratio of bond strength to parent metal strength, σ_b/σ_{pm} , to the ratio of the applied bond pressure (P_a) to calculated bond pressure, P_a/P_b . The reported strength values were determined by shear as well as tensile tests and both are presented. These results are presented for the numerous data in Figures 5 and 6 for the plane strain and plane stress conditions respectively. Because of the wide range of values of P_a/P_b , these data are presented on semi-log graphs. Some of the representative data are presented below in Table I.

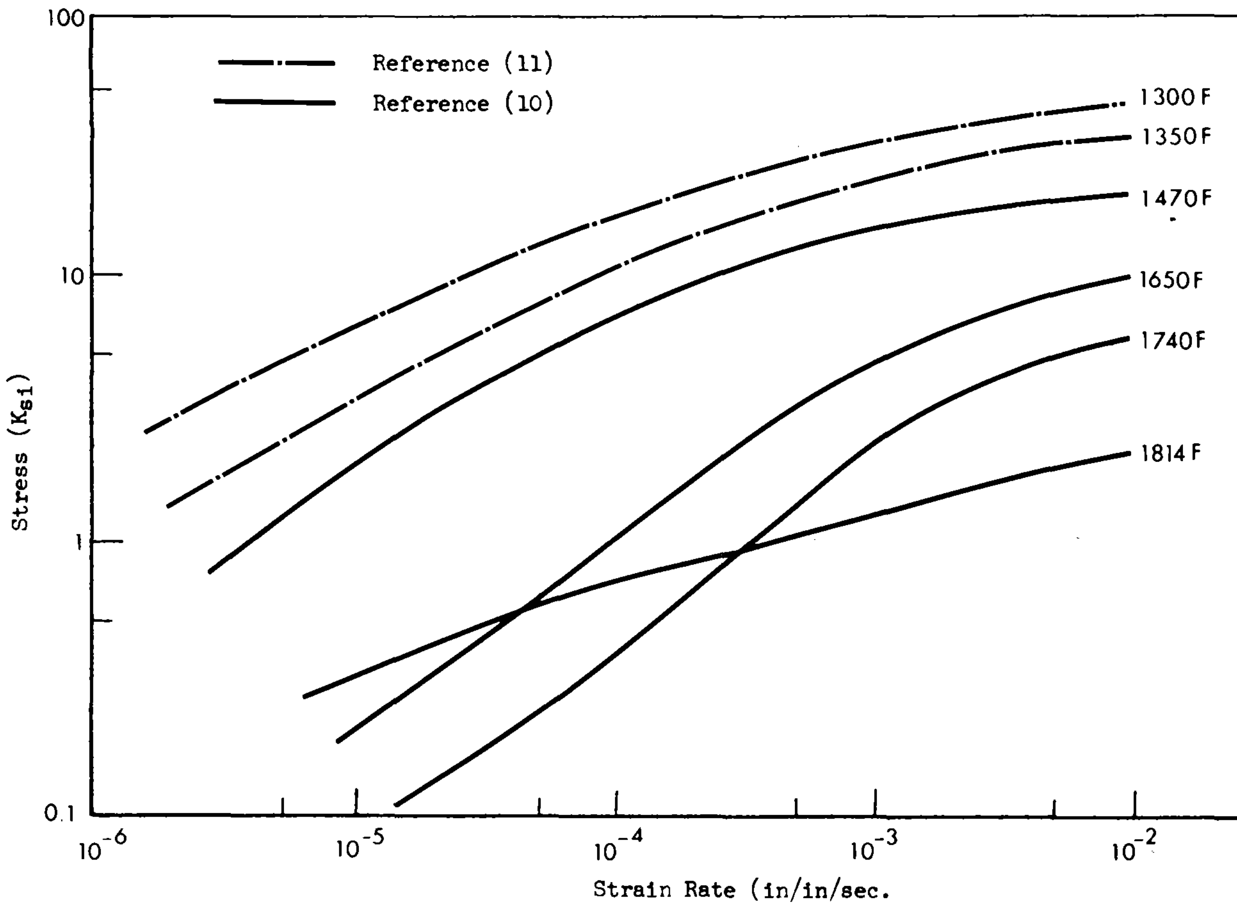


Fig. 4. Flow stress as a function of strain-rate for Ti-6Al-4V at several temperatures.

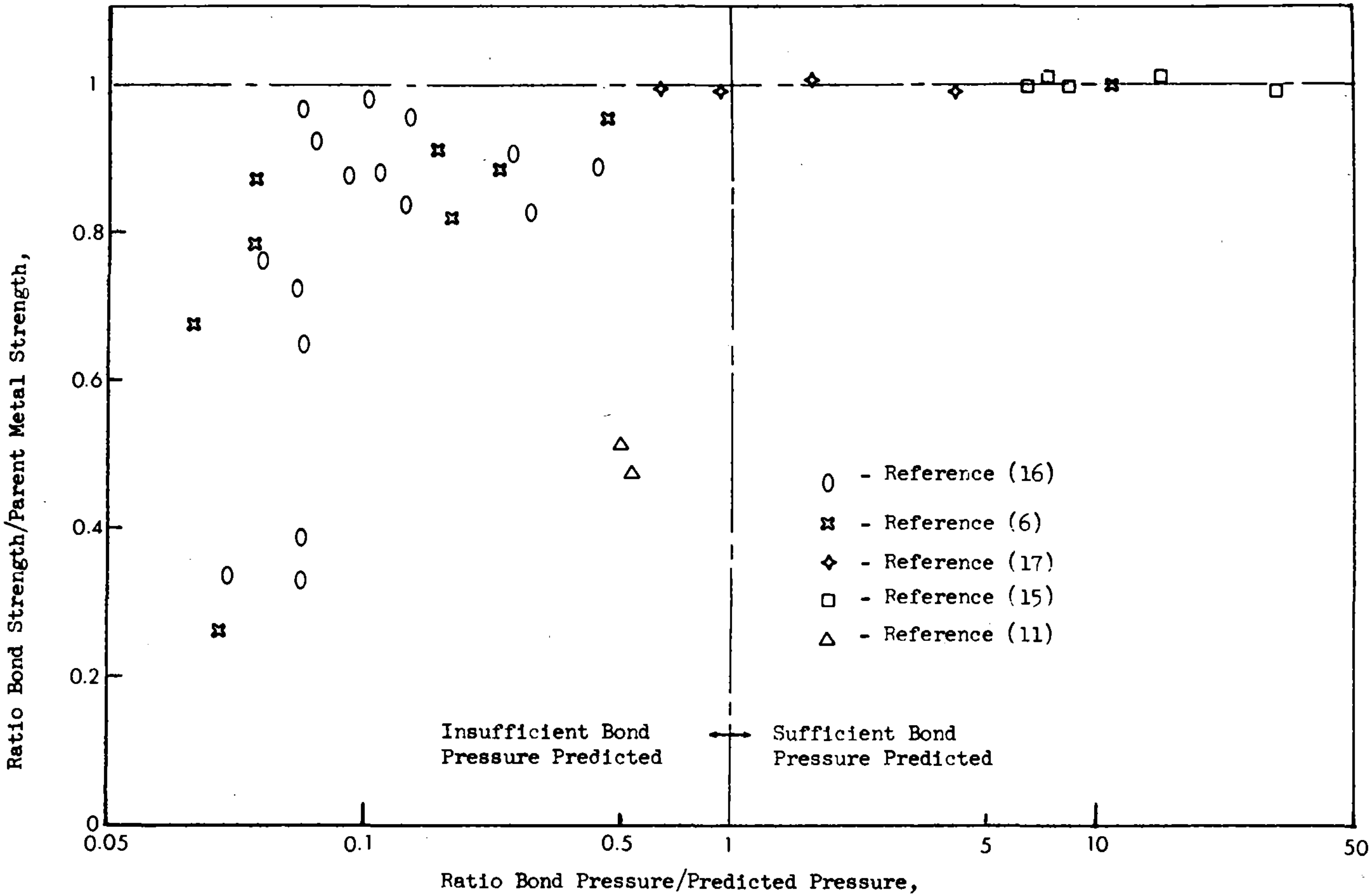


Fig. 5. Comparison of the ratios of bond strength-to-parent metal strength with bond pressure-to-predicted pressure for assumed plane strain deformation.

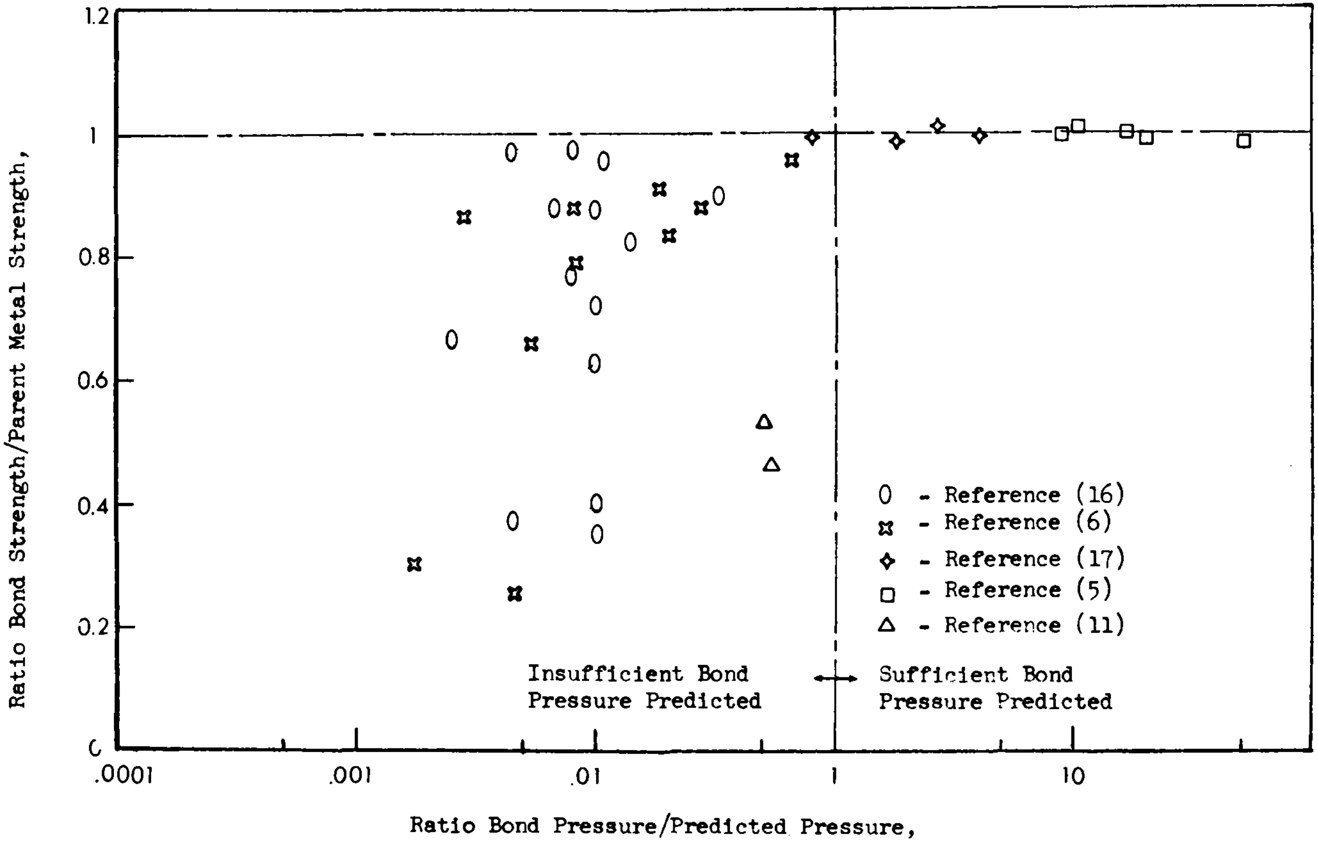


Fig. 6. Comparison of the ratios of bond strength-to-parent metal strength with bond pressure-to-predicted pressure for assumed plane stress condition.

Table I

Comparison of Analytical and Reported Experimental Data For Diffusion Bonded Ti-6Al-4V

Bond Temp. (°F)	Bond Time (t_b)	Bond Press., P_a (psi)	Pred. Press., P_b (1) (psi)	Pred. Press., P_b' (2) (psi)	P_a/P_b	P_a/P_b'	Bond Strength σ_b (ksi)	σ_b/σ_{pm}	Ref.
1350F	45 Min	5,000	9,300	7,500	0.54	0.666	72	0.47	11
1440	60 Min	100	6,400	5,000	0.016	0.02	28(s)	0.304	6
1540	60 Min	650	2,900	2,250	0.23	0.28	81(s)	0.89	6
1400	400 Min	300	3.800	3,000	0.078	0.10	79.4(s)	0.88	16
1650	96 Min	50	695	600	0.072	0.08	76.3(s)	0.78	16
1650	230 Min	50	400	350	0.12	0.14	81.2(s)	0.83	16
1675	960 Min	2,000	145	100	14.0	20.0	143	1.02	15
1600	960 Min	2,000	290	200	6.9	10.0	132	1.02	15
1700	60 Min	100	640	500	0.16	0.19	133	0.92	6
1700	240 Min	100	230	150	0.42	0.67	137	0.948	6
1700	180 Min	2,000	290	200	6.9	10.0	141	1.007	15
1700	960 Min	1,000	120	80	8.3	18.2	140	1.0	15
1750	15 Min	25	1,450	1,000	0.017	0.025	59.6(s)	0.67	16
1750	960 Min	2,000	60	40	33	50.0	137	0.978	15

(s) - Block Shear Test

(1) - Plane Strain Condition
(2) - Plane Stress Condition

The data evaluated show excellent agreement to the analytical predictions. For values of P_a/P_b greater than 1, the analysis predicts complete closure of bond surfaces, and the data indicates generally full parent metal strength when this condition is met. For values of P_a/P_b less than 1, the analysis suggests incomplete closure of bond surfaces and therefore attendant bond line porosity and reported bond strengths are generally less than parent metal strengths for this condition.

Although plane stress pressure requirements calculated are 70 - 85% of the plane strain pressure requirements, predictions based on either assumed strain condition were equally successful in separating low strength from the full parent metal strength bonds. This may be because of the extreme variations in pressures used relative to the predicted, and the corresponding sparseness of available data developed with a bond pressure close to the predicted values. It appears, however, that either analytical approach would be effective in providing a rough prediction of pressure required.

Additional Experimental Study

In order to further evaluate bond pressure requirements at levels close to predicted values, additional samples were diffusion bonded. Cylinders of 0.75 in. diameter by 1.150 in. in length were machined from 1 in. thick plate of Ti-6Al-4V alloy. The surface finish of the bond surface at the cylinder ends was measured to be 180 RMS. The composition of the alloy is presented in Table II.

Table II

Alloy Composition of the Ti-6Al-4V
Used in the Diffusion Bond Study

Alloy Element	Al	V	C	Fe	O	N	H
Content (%)	6.5	4.2	0.026	.17	.17	.009	.004

The cylinders were diffusion bonded under vacuum of better than 10^{-4} TORR and under various time, temperature and pressure parameter combinations. The resulting bonded specimens were machined into tensile specimens such that the bond was at the center of the test section, and metallographic specimens were taken to reveal the bond line immediately adjacent to the test section. Microporosity in the bond line was determined metallographically as a percent of bond line length. Bond pressures requirements for plane strain condition were computed as discussed previously and bond pressures used were chosen to be near the computed values.

The bond parameters, computed pressure requirements, resulting tensile data, and interface void concentration are presented in Table III.

Table III

Comparison of Analytical Pressure Predictions
to Experimental Results

Spec. No.	Temp. (°F)	Bond Time (Min)	Bond Press. (psi)	P_b (psi)	P_a/P_b	Strength Ult. (ksi)	Strength Yield (ksi)	Elong. (%)	Red. of Area (%)	Voids (%)
1	1650	120	725	580	1.25	138.8	-	0	0	40
2	1600	120	2,500	2030	1.23	141.4	140.4	0	0	48.9
3	1600	120	3,500	2030	1.72	155.3	143.9	5.7	9.8	6.7
4	1500	120	1,000	7000	0.25	132.0	-	0	0	61.3
5	1600	60	3,500	2600	1.34	147.6	139.8	1.42	6.3	36.7
	(Parent Metal)					152.2	141.4	14.5	31.3	

The pressures applied, with the exception of specimen No. 4, were slightly in excess of the predicted value although the resulting properties and metallographic analysis (see Figure 7) indicate incomplete bonding in all samples. It is apparent from these data that the elongation and reduction of area are more sensitive indications of incomplete bonding than tensile strength alone. Plane stress assumptions would predict lower pressures than the plane strain assumption, and therefore would fall farther from the actual requirement.

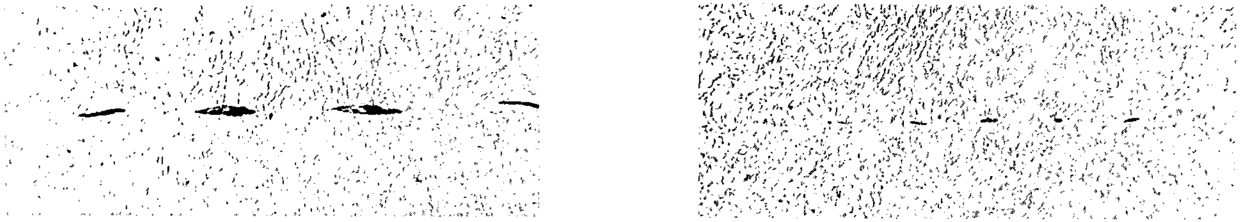
The inability of the proposed analytical method to more closely predict the required pressure in this study may be related to any of a number of possible factors. The assumptions of average strains and average stresses, the assumption of asperity geometry, or the applicability of the high temperature property data. The latter factor may be a major consideration since the data used here (see Figure 4) was developed by tensile loading of sheet material to establish the strain rate characteristic of the material, whereas the bonding tests were conducted on specimens machined from plate stock under compression. Recent data (12) indicates a strong dependence of flow stress on the grain size of Ti-6Al-4V and hence microstructural variations may contribute to a wide range of strain rate responses.

The results of other recent tests (13) to establish stress/strain-rate characteristics of plate material under compression loading substantiate this possibility. These data, at 1650°F, show a substantial increase in flow stress over previously reported data as shown in Figure 8. Using these data to re-check pressure predictions of specimen No. 1 (Table III), the results indicate a required pressure of 1680 psi which is well above the 725 psi used and agrees with experimental results which reveal lack of sufficient pressure. This result suggests that the analytical predictions proposed herein may be relatively accurate provided valid material deformation data are available for the temperature under consideration.

Discussion

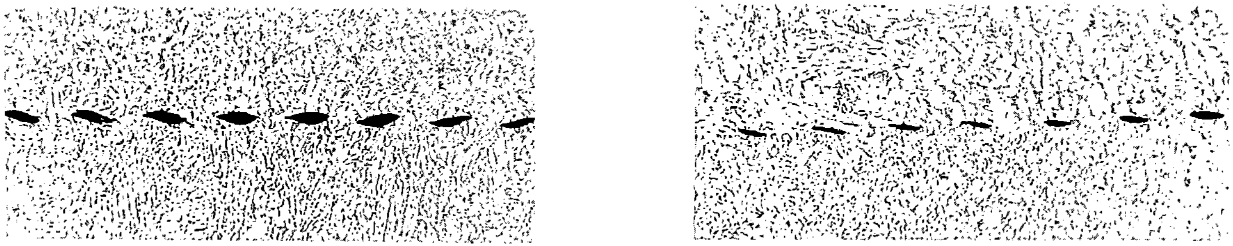
Although the exactness of pressure predictions has yet to be fully evaluated, it does appear to provide a realistic assessment of the trade-offs existing among the primary bond parameters of pressure, time, and temperature (as reflected by the material creep properties). Consequently, the predicted bond pressure requirements can be evaluated as a function of bonding time through these calculations. Using the creep data of Figure 4, such calculations based on the plane strain assumption result in the pressure vs. time curves shown in Figure 9 for the temperatures of 1470F, 1650F, 1740F, and 1814F.

Specimen No. 1 100X



Specimen No. 2 100X

Specimen No. 3 100X



Specimen No. 4 100X

Specimen No. 5 100X

Fig. 7. Bond line characteristics of specimens diffusion bonded with various parameters.

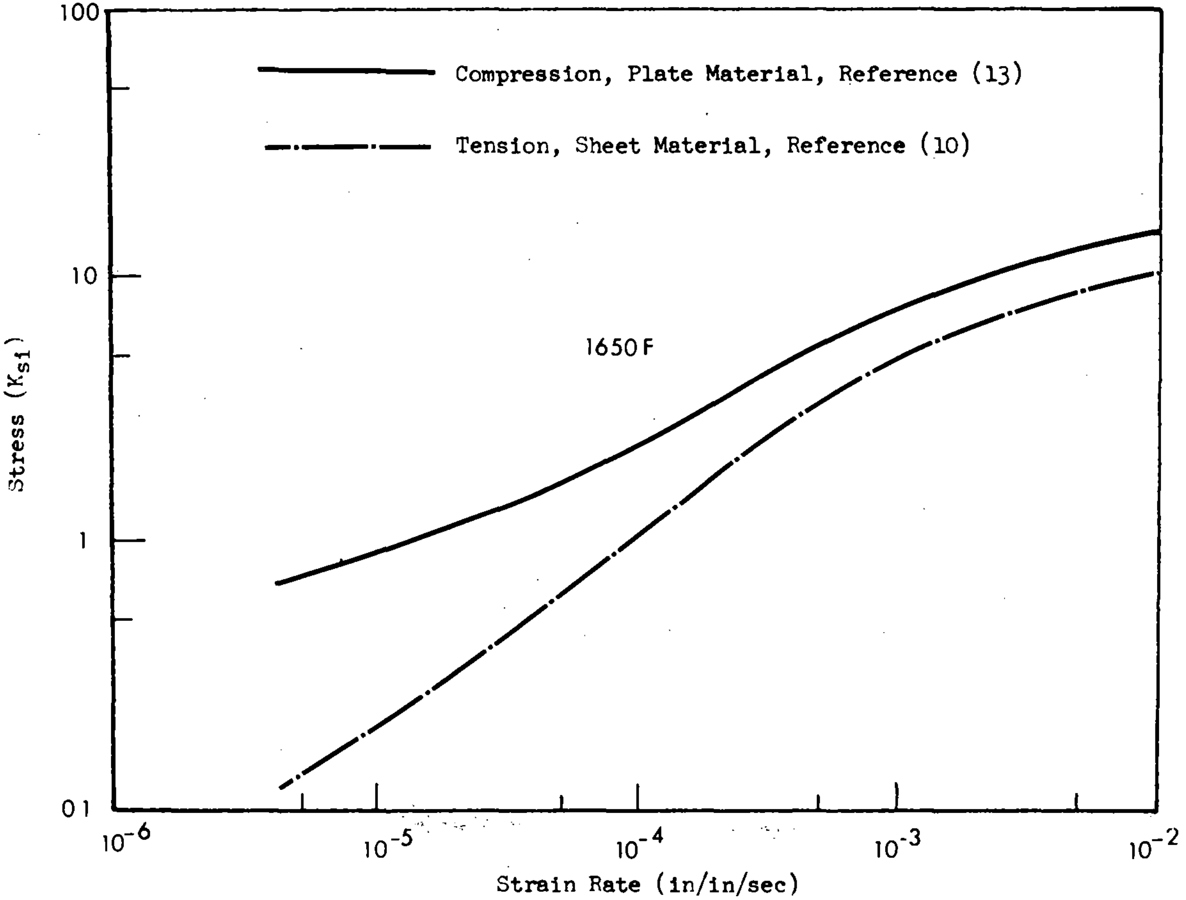


Fig. 8. Flow stress as a function of strain-rate for compression loaded plate material and tension loaded sheet material.

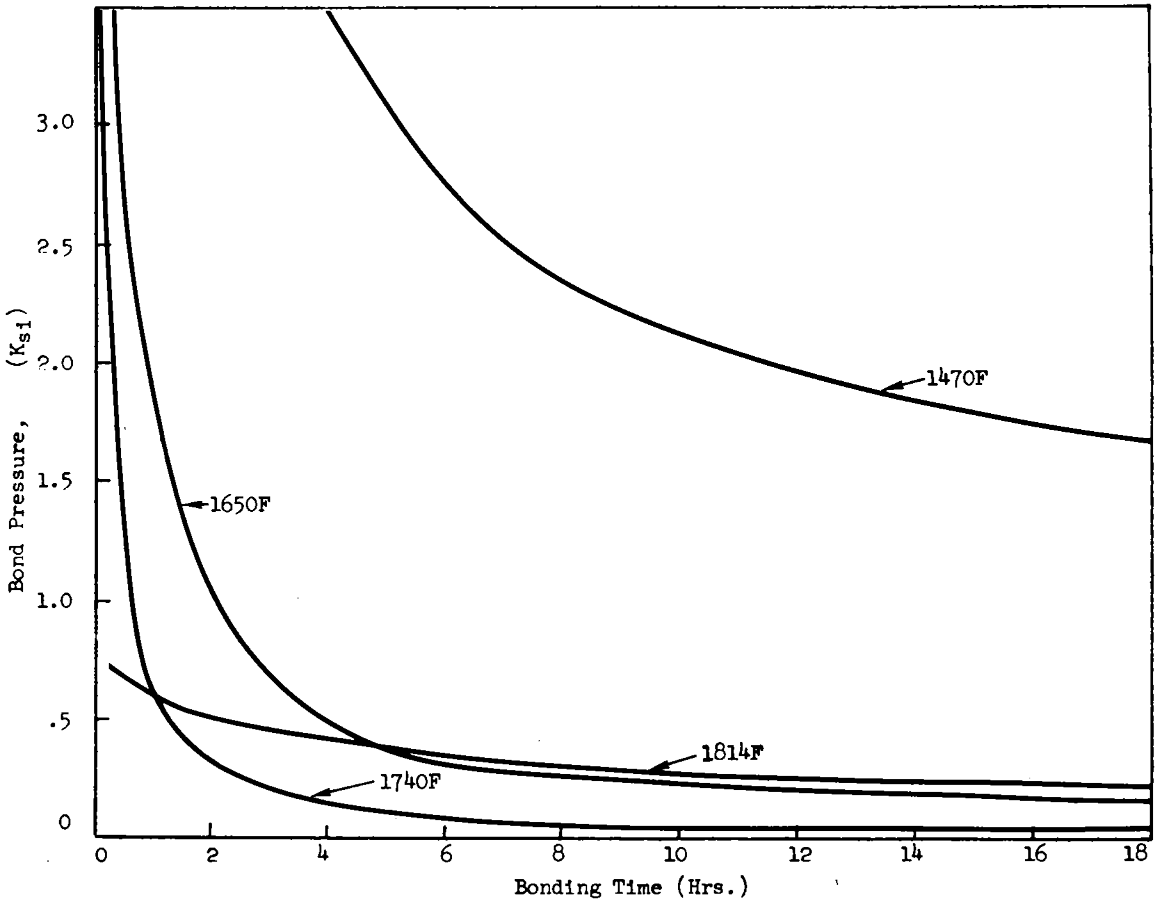


Fig. 9. Bond pressure requirements as a function of bond time as determined analytically.

Of particular interest is the precipitous increase in the predicted pressure required as bond time decreases below about 3 or 4 hours for temperatures of 1650F to 1740F. However, pressures for bonding at 1814F, which are generally higher than the 1650 - 1740F range for the longer bond times, do not rise so rapidly as bond times are decreased. This effect is reflective of the corresponding change in stress vs. strain-rate curve for 1814F in Figure 4.

The pressures for these high temperatures appear to become generally insensitive to bond time variations for times above 4 or 5 hours, and indicates that little is to be gained in terms of surface creep by holding for longer times.

The very low bond pressures predicted for 1740F agree with the results of a prior study (2) which led to the conclusion that 1750F provides for maximum surface creep.

Although this analysis deals with only one aspect of a somewhat complex process, it is surprisingly accurate in assessing the sufficiency of bond parameters over a wide range of conditions. The limitations of the applicability of surface creep deformation analysis have yet to be established, however. For example, as the temperatures and/or times of bonding are reduced below some critical values, closure of the interface by surface creep will not be sufficient to permit complete bonding since the required diffusion-aided bonding mechanisms become increasingly impaired.

It is of interest to note that the actual asperity height, or machine surface finish, does not enter the calculations. Rather, it is the average strain of the asperity which is constant, regardless of height, provided the assumption of symmetry about the resulting bond plane is met. That is, the average strain and resultant average strain-rate are constant for variable size asperities deforming under a given applied pressure. While this should be valid within the confines of the analysis (i.e., analysis of creep deformation only), it does neglect other aspects of the solid state bonding process which may aid formation of full bond area contact. For example, it has been demonstrated (5) that bond line microporosity can be eliminated by holding for a sufficient period of time at elevated temperature in the absence of pressure. Such an effect is apparently caused by diffusion of vacancies away from the pore as driven by the surface energy associated with a micropore (14), and thereby contributing to its collapse. This rate of void shrinkage is related to the inverse square of the radius, and therefore becomes an increasingly important factor as the micropore size becomes smaller.

Because of the void shrinkage effect, it is likely that the size of asperities, or surface finish, will be a factor in the total time to form complete and intimate contact over the interface. Consequently, it may be expected that this effect will contribute to the bonding much earlier for a fine surface finish than for a rough finish since the resulting voids at any given time will be smaller for the case of the fine finish. Such an effect of surface finish has been observed in prior studies (1, 2 and 6) where more rapid bonding was observed for finer surface finishes.

Since this annealing-out of micro-voids actually aids in achieving complete bonding, the predictions based on analysis of surface deformation alone should be somewhat conservative for predicting full bond line contact for finer surface finishes, higher temperatures, and/or longer bond times.

The analysis was developed in general terms, without regard to the material, and therefore should be applicable to other metals and alloys provided the proper mechanical property data are available. It is recognized, however, that the pressure requirements to overcome surface asperities is only one, albeit necessary, aspect of diffusion bonding and other conditions must also be met if a good bond is to be achieved.

The inaccuracy of the analysis observed in the additional bond studies, and the subsequent agreement observed with creep data determined by compression testing plate material, indicates the need for deformation data that is truly representative of the material being bonded. Since metallurgical factors such as grain size or structure can substantially affect the deformation characteristics of Ti alloys at elevated temperatures, pressure predictions based on the reported data must be considered approximate.

With creep data truly representative of the material to be diffusion bonded, it would then be possible to conduct a more critical check of the simplifying assumptions employed in the analysis. Nonetheless, the existing data used coupled with the analysis presented herein was demonstrated to provide a valuable tool for considering the parametric trades which exist over a wide range of the pressures, times, and temperatures for diffusion bonding Ti alloys.

Conclusions

1.) Interface deformation under applied pressure is a necessary and important aspect of diffusion bonding.

2.) The pressure required to overcome interface asperities by creep deformation has been approximately described through a relatively simple analysis utilizing average rates of strain and stresses resulting from applied pressure. Predictions based on this analysis were shown to agree favorably with reported data developed with a wide range of pressure, time, and temperature combinations.

3.) Prediction of pressure requirements based on a plane stress state assumption are approximately 70-85% of predictions based on a plane strain state assumption, but insufficient reliable test data are available within this range to establish conclusively that one assumption is the more suitable.

4.) Predictions of pressure-time combinations indicate a very sharp drop in pressure required as time is increased to about 3 or 4 hours, and virtually no change after this for temperatures of 1650-1740°F.

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