THEORY OF TRANSPORTING SOLIDS IN

AIR AND GAS DRILLING

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CHAPTER I

INTRODUCTION

The use of air and gas in rotary drilling was introduced just prior to 1950 and its use was expanded during the 10 years from 1950 to 1960. During the present decade its use has reached a static condition relative to expansion. Because in the use of any technique static conditions are generally short-lived, it must be assumed that the use of air and gas will either decline or increase in the future. Thus the purpose of this research has been to expand old technology and introduce new ideas that will lend themselves to a further expansion of the use of air and gas in rotary drilling.

Drilling with air and gas was expanded rapidly in the early 1950's, when it had been demonstrated that penetration rates could be increased by a factor of 10 over penetration rates with drilling mud. However, the drilling industry faced problems. Drilling rigs were burned because of escaping gas around the rig floor, underground explosions resulted from combustible mixtures of air and hydrocarbons under pressure, and underground water flows resulted in stuck drill pipe. Of equal importance was the fact that volume requirements were an unknown quantity. From this beginning, solutions were found for the most serious problems. Better pack-off equipment minimized escaping gas and safety precautions minimized the danger of fires. The combustion pressure range for various concentrations of air and hydrocarbons was determined and used by rig crews when potential problems existed. Methods for controlling water flows were conceived. In some cases attempts were made to shut-off the influx of water; in others, chemicals and water were used to form a bed of foam to remove the water.

In all of this development, volume requirements for air and gas drilling were never considered a real problem. At first the drilling industry was content to use a modification of Weymouth's equation for horizontal air flow in pipe-lines. Improvements were made and in 1957 a set of charts for selecting air and gas volume requirements were introduced by Angel (1). This work represented a big step forward, but unfortunately the last step forward. Industry was still left with the necessity of assuming standard air velocities of 3,000 ft/min for annulus flow, a value taken from the mining industry. Density effects of formation solids were considered relative to pressure losses and neglected relative to particle slip velocities.

From this beginning the drilling industry has drifted into the habit of determining air and gas requirements on a trial and error basis. Past experience has become the determining factor; the application of sound technology is disappearing from field practice. If this happens, air and gas drilling will also disappear. Thus, this research is the first step in the necessary revival of technology in air and gas drilling.[1]

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[1]For the purpose of clarity and to prevent a continued use of the terms air and gas, only the term gas will be used in the following chapters.

CHAPTER II

SUMMARY OF PREVIOUS WORK

The review of previous work will be directed towards those areas that concern technology related to volume requirements in gas drilling. This will include work where: (1) the primary objective was to develop methods for determining gas requirements in rotary drilling, (2) the objectives were limited to particle lift in areas related to rotary drilling, (3) the objectives were concerned with particle lift in areas unrelated to rotary drilling and (4) the objectives were to determine pressure losses in multiphase solid-fluid systems.

Because the use of gas in rotary drilling is relatively new, the development of technology in this area is limited. Martin (2) in 1952 and 1953 presented methods to determine gas volume requirements based on a modification of Weymouth's equation for gas flow in a horizontal pipe line. His results are shown in Equation (1).

$$Q_s = \frac{275\ T_s\ D\ (P_1^2 - P_2^2)^{1/2}}{P_s\ (h\ S\ T_a)^{1/2}} \qquad (1)$$

There is no gravity term for vertical flow and the presence of drill solids have been ignored. Because of the deficiencies in Equation (1), it was obvious immediately that some other method would have to be utilized. Beginning with Martin's work the drilling industry used a rule-of-the-thumb gas velocity for cleaning the hole of formation cuttings. The velocity assumed was 3,000 ft/min of standard air.[1] This number came from quarry mining and was an experience factor. It seemed to suffice and is still accepted by the oil industry.

Nicolson (3) in 1954 presented an equation for slip velocity based on Newton's second law, F = ma, and a constant drag coefficient of 0.5. His results are shown in Equation (2).

$$v_s = 2.67 \left[\frac{D_p \rho_p}{\rho_f} \right]^{1/2} \qquad (2)$$

Nicholson's equation was recognized but not used. Industry had decided to use the velocity of 3,000 ft/min of standard air. Actually his assumption of 0.5 for the drag coefficient on spheres was based on work from other areas and is approximately correct for a sphere which is dropped through a quiescent fluid in an infinite gas column.

In 1957, four other investigators, Gray (4), McCray and Cole (5) and Scott (6) made contributions to drilling with gas. Gray used the same approach as Nicolson. However, instead of assuming a drag coefficient of 0.5, Gray's work was directed primarily towards the measurement of actual drag coefficients. He used limestone and sandstone particles and found the drag coefficient for sandstone to be an average of 0.805 and for limestone to be 1.400.

The difference in drag coefficients was assumed to be a function of particle shape. Sandstone particles were primarily subrounded in shape and limestone particles were angular in shape. These runs were made with a given mass of particles which were suspended in a transparent flow chamber. He used the slip velocity equation developed

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[1]Standard air refers to air at 14.7 psia and 60°F.

for spheres and calculated drag coefficients making the assumption that the measured drag coefficients provided the necessary correction for particle shape.

Gray used the slip velocity formula derived from taking a force balance on a sphere, which is shown as Equation (3).

$$v_s = \left[\frac{4\,(\rho_p - \rho_f)\, g\, D_p)}{3\,\rho_f\, C_D}\right]^{1/2} \qquad (3)$$

He considered wall effects by using Equation (4) first introduced by Bruce and Williams (7).

$$v_a = \frac{v_s}{1 + \frac{D_p}{D}} \qquad (4)$$

Using Equations (3) and (4) and his experimentally determined drag coefficients, Gray found that his calculated slip velocities deviated from the actual slip velocity by an average of 6.49 percent for sandstone and 7.10 percent for limestone. He assumed that shale cuttings would be angular and similar in shape to the limestone particles and that the drag coefficient for shale would also be 1.40.

Brown (8) suggested the use of a sphericity factor to correct for particle shape. The suggested sphericity factor, x, is shown in Equation (5).

$$x = \frac{D_a}{D_s} - \frac{1}{n} \qquad (5)$$

In this Equation (5), D_a is the average diameter of the particle or particles determined from a screen analysis; D_s is the equivalent diameter of a sphere having the same volume as the particle; n is the ratio of specific surfaces. Gray did not use the sphericity factor because of the difficulty involved in the determination of n. By using the ideal gas law for density and the drag coefficients determined for limestone and sandstone, Gray modified Equation (3) to give the following slip velocity equations:

For Limestone and Shale:

$$v_s = 0.9456 \left[\frac{D_p T_a \rho_p}{P}\right]^{1/2} \tag{6}$$

For Sandstone:

$$v_s = 1.445 \left[\frac{D_p T_a \rho_p}{P}\right]^{1/2} \tag{7}$$

Equations (6) and (7) were used by Gray to construct Fig. 1 and 2, which show slip velocity as a function of bottom-hole injection pressure. If observed casually, this might indicate that the gas volume required for lift goes up as the pressure goes down. Actually the reverse is true, an increase in pressure will result in the requirement of a greater volume of gas. However, Gray's results are useful for the determination of drag cofficients and are believed to be the best work available for this purpose. Either Equation (6) or (7) could also be plotted as a straight line on log-log paper with a slope of minus one-half. Such a modification of Equation (6) is shown as Equation (8).

$$\text{Log } v_s = \log (0.9456\ D_p T_a \rho_p)^{1/2} - 1/2 \log P \tag{8}$$

Gray's work used alone does not provide a method for calculating volume requirements, but is a valuable aid to work of this type.

Also in 1957, a method for calculating gas requirements was presented by Angel. He included charts which were the result of computer

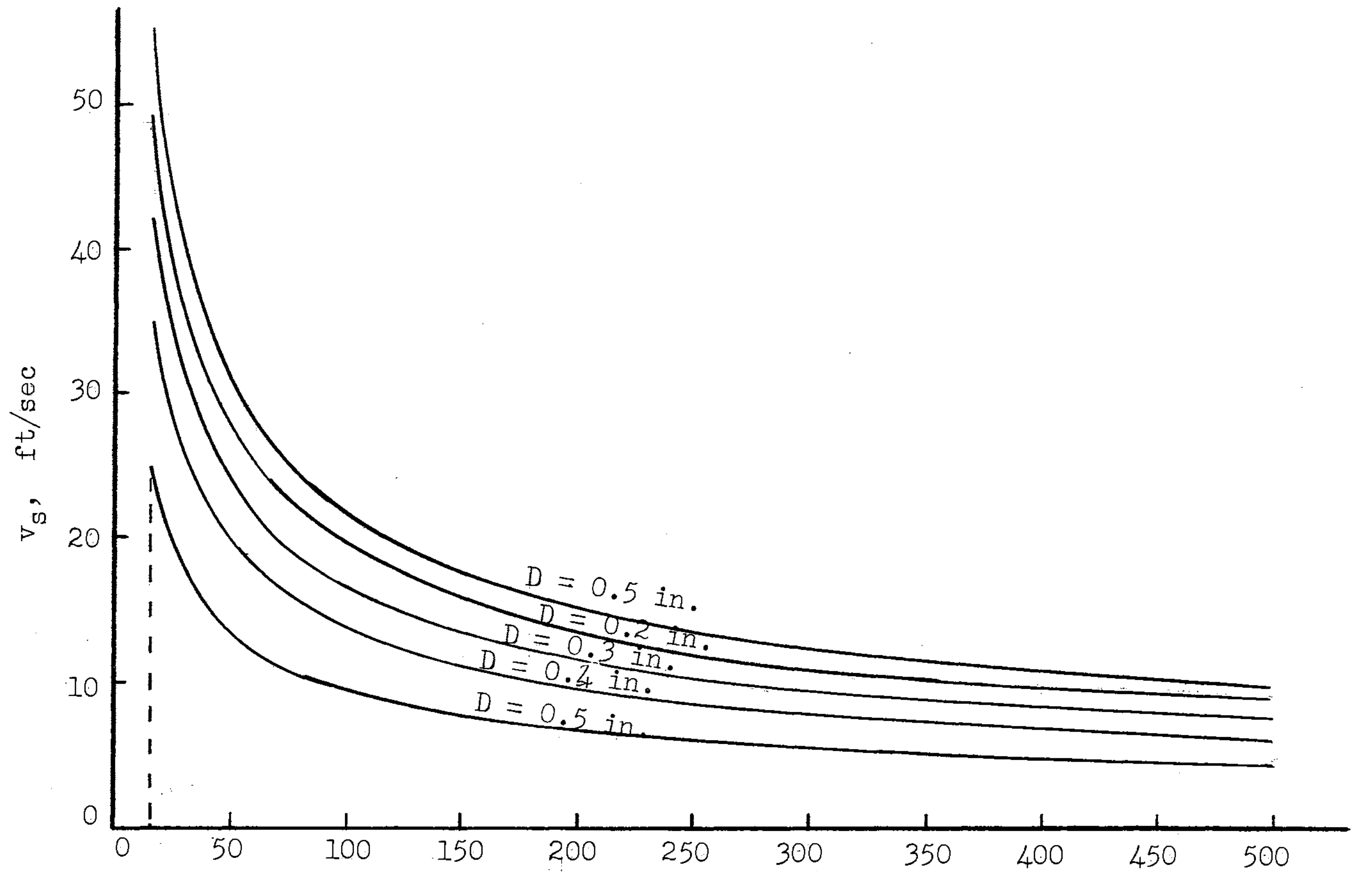


Fig. 1. Slip Velocity vs Bottom-Hole Injection Pressure For Limestone & Shale

From Gray (4)

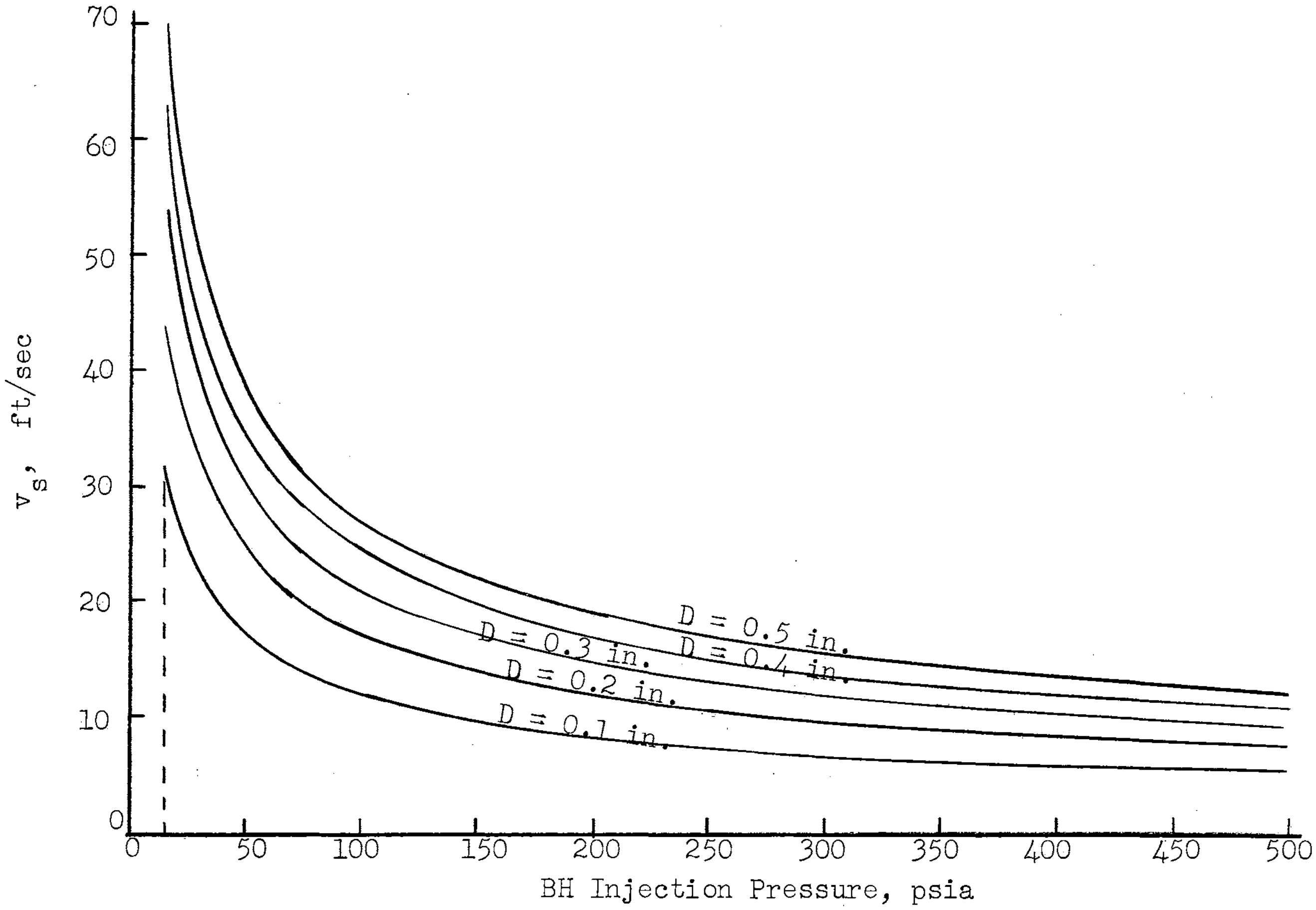


Fig. 2. Slip Velocity vs Bottom-Hole Injection Pressure for Sandstone

From Gray (4)

solutions, and because of the fact that these data could be readily used they were adopted by the oil industry. Angel used two equations, one to predetermine the pressure at any point in the annulus and the slip velocity formula shown as Equation (3). However, he did not use Equation (3) to determine gas requirements. Instead he also assumed that the required velocity of standard air was 3,000 ft/min. Because Angel's work introduced a new approach, the basic method that he followed will be outlined.

To determine pressure loss at any point in the annulus, he used Equation (9), which is a simplified form of the general energy balance.

$$dP = \rho_m \left[\frac{g\, dh}{g_c} + \frac{v^2 f \; dh}{2g_c \, (D_h - D_d)} \right] \qquad (9)$$

The first term on the right hand side of Equation (9) is the static head and the second term accounts for the flowing pressure losses. This is a standard equation and is used frequently to determine the bottom-hole pressure in dry gas and gas condensate wells. Angel used the Weymouth friction factor of 0.014 $(D_h - D_d)^{-1/3}$ in Equation (9). Actually there are many empirical constant friction factors in use. In normal pipe line flow the Weymouth friction factor has been found to be as accurate for determining pressure losses as more complicated methods. The Moody friction factor could be used. However, Fig. 3 shows a plot of the Moody friction factor versus Reynolds number and in a high range of Reynolds numbers the friction factor is a function of pipe roughness. For most cases it would be very difficult to describe the roughness in the drill pipe-hole annulus. For this reason the use of a constant

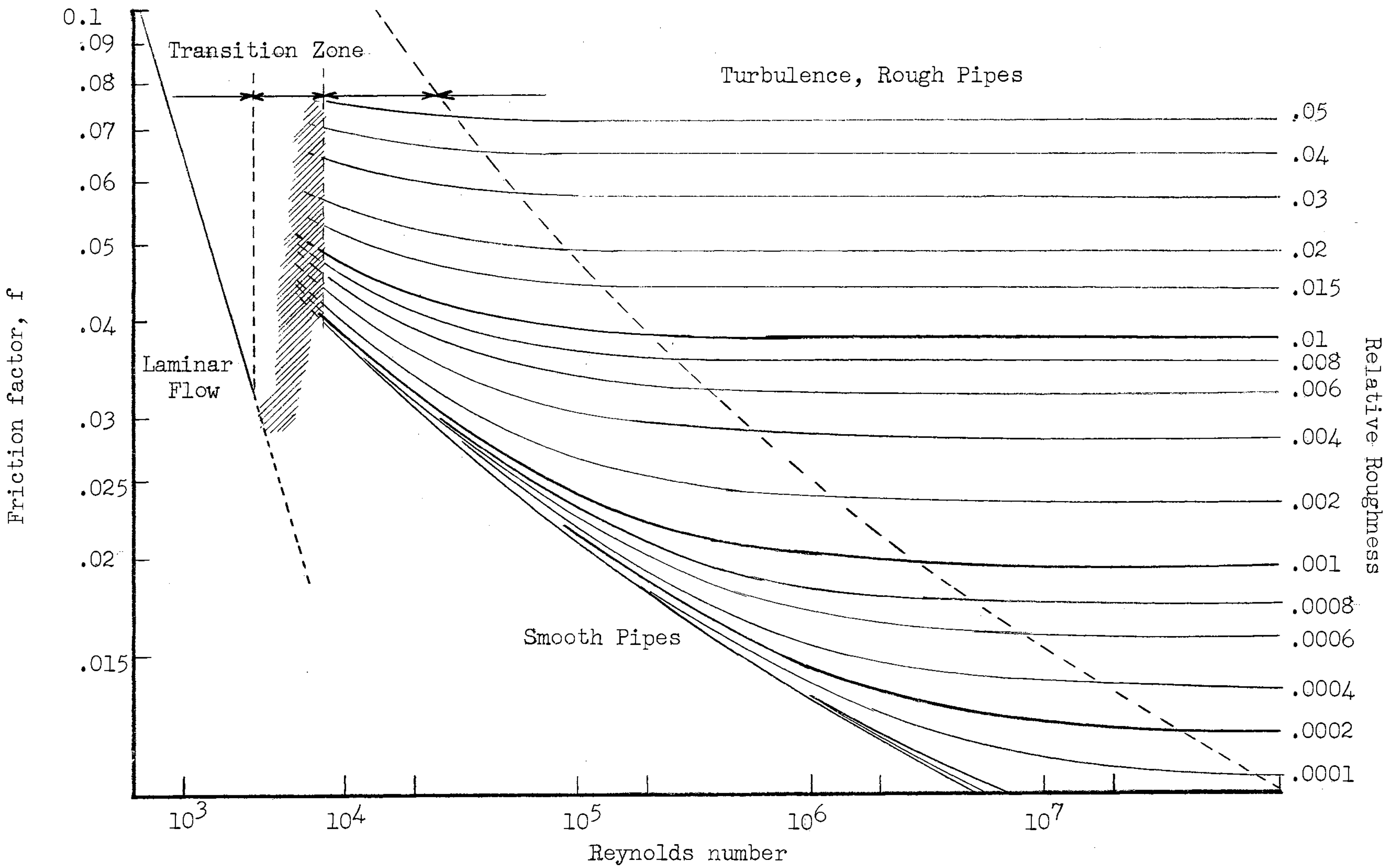


Fig. 3. Moody's Friction Factor vs Reynolds Number

friction factor such as the Weymouth friction factor simplifies the calculation with no particular sacrifice in accuracy.

Angel modified the density term as had been done when the gas stream contained liquids. This was done as shown in Equation (10).

$$\rho_m = \frac{SP}{53.3\ T_a} \left(1 + \frac{M_p}{M_f}\right) \quad (10)$$

This modification was made to account for the change in fluid density introduced by the presence of drill solids. In making this modification the following assumptions are necessary:

1. The solids from the formation go into solution.
2. The resulting solution is homogeneous in nature.

Equation (10) allows for the consideration of drilling rate as a parameter, since the density is changed in proportion to the quantity of solids added to the gas stream and the quantity of solids added to the gas stream is a direct function of the drilling rate.

Angel used a temperature variation based on the geothermal gradient and then settled on the use of an average temperature, because it could be shown that the results using either approach were essentially the same.

In conjunction with Equations (9) and (10), Angel used a modification of Equation (3). He assumed a required standard air velocity of 3,000 ft/min, and also used the continuity equation to obtain a second equation which contains pressure as one of the variables. His modification of Equation (3) and his use of this modification with the one-dimension continuity equation introduced an incompatible mathematical relationship that was not valid. By considering the drag coefficient,

C_D, as a constant and neglecting the density of the fluid relative to the particle density, Equation (11) is obtained from Equation (3).

$$\rho_s v_{ss}^2 = \rho_f v_s^2 \tag{11}$$

As long as the slip velocity is used in Equation (11), it is limited only by the assumptions made. Although Angel's work does not include the specific assumptions made, the following assumptions were necessary for his final equation, in which he substituted the fluid velocity for slip velocity in Equation (11).

1. The most difficult point of lift was at standard conditions or in this case at the surface. This was done when he assumed the standard air velocity equal to the slip velocity.
2. The air velocity required to lift cuttings at any other point in the hole can be defined by the modified form of Equation (11).

None of these assumptions are considered valid. The one dimensional continuity equation shown as Equation (12) was also used.

$$\rho_s A v_{ss} = \rho_f A v_s \tag{12}$$

This equation states that $\rho_s v_{ss} = \rho_f v_s$. Thus if Equation (12) is used with Equation (11), the assumption must be made that the density of the air remains constant with changes in temperature and pressure. This is an obvious discrepancy which makes this approach mathematically incorrect.

Combining the integrated form of Equation (9) with Equations (10), (11), and (12) formed the basis for Angel's final formula for gas volume requirements shown as Equation (13).

$$6.61 \; S \; T_a \; Q_s^2 \;=\; \left[(P_s^2 + b \; T_a^2) \; \rho_f^2 \; {}^{ah/T_a} - b \; T_a^2 \right]^{1/2} \qquad (13)$$

where:

$$a = \frac{SQ_s + 28.8 \; r \; D_h^2}{53.3 \; Q_s}$$

$$b = \frac{1.625 \times 10^{-6} \; Q_s^2}{(D_h - D_d)^{1.333} \; (D_h^2 - D_d^2)^2}$$

Angel solved Equation (13) by trial and error methods. He reasoned that a trial and error solution represented a more simplified approach than solving for Q_s, since this term appears in quadratic form in both sides of Equation (13). Since he introduced the assumption of 3,000 ft/min of standard air, and used equations which contain mathematical inconsistancies his results are of questionable accuracy.

Also in 1957, Scott presented an approach similar to that proposed by Angel but not as clearly illustrated. He began his work using a general energy balance and developed the same differential pressure equation used by Angel, except he used the Fanning friction factor in the final form of his equation. He also used the assumption that a standard air velocity of 3,000 ft/min was required to lift cuttings and used the same approach as that used by Angel to include this velocity into his final equation for volume requirements. Since Scott's development is the same as that taken by Angel, his equations and work will not be presented.

Following this work McCray and Cole in 1958 used a similar approach to that used by Angel and later by Scott. They started with the general energy balance and developed the same equation as that shown in Equation (9), except the kinetic energy term was not omitted in their approach. Also the Fanning friction factor was used instead of the Weymouth friction factor. This followed the approach taken by Scott. Actually the use of the kinetic energy term, which can be shown to be negligible, and the Fanning friction factor is not believed to increase accuracy but it greatly increases the difficulty of the calculation. McCray and Cole used the same density correction factor as that used by Angel. They used the force balance in exactly the same manner as Angel and Scott. Thus their final results would differ from those used by Angel only by the change in the predicted pressure at any point. Because the pressures predicted by McCray and Cole's equations are generally higher than those from Angel's equation, their required gas volumes are also higher, particularly as wells are drilled deeper.

The work cited has been a resume of the most significant contributions used by the oil industry for the determination of volume requirements in gas drilling. The resume of work is necessarily short, because there has not been a dedicated effort by research organizations in this field. Although the literature contains a large volume of material on gas drilling as an art, there has been little interest in trying to develop the science. Angel's work in 1957 brought forth the last new idea to be considered in gas drilling; McCray and Cole's work followed in 1958 but they used the same appraoch as that used by Angel. Actually a large volume of work has been done on particle lift and drag coefficients but this work has not been considered in the oil industry. For this reason, the remaining portion of this review of past work will be directed towards a summation of the more significant work that has been performed mostly for other industries and not used in the oil industry.

One of the most significant contributions on particle lift made to technical literature was that provided by Lapple and Shepherd (9). They combined the data of several investigators to prepare a correlation of drag coefficients versus particle Reynolds number for spheres and discs. Their correlation is shown in Fig. 4. In this plot the particle Reynolds number is defined by Equation (14).

$$R_p = \frac{1488\ \rho_f v_s D_p}{\mu} \qquad (14)$$

The drag coefficient is the same as that shown in the development of Equation (6). Fig. 4 shows three regions of interest. These regions are summarized as follows:

1. The Stokes Law region, $R_p < 1.0$.
2. The intermediate region, $1.0 \leq R_p < 300{,}000$.
3. The turbulent region, $300{,}000 < R_p$.

This summary of drag coefficients applies for Newtonian fluids only and is valid only for single particles in an infinite stream. Although deviation from Newtonian behavior must be considered when using these drag coefficients, gas will behave as a Newtonian fluid thus these data are applicable under similar conditions. In the normal gas drilling operation, the area of interest is the region where the drag coefficient remains constant. It will be noted that drag coefficients for spheres in this region are about constant at 0.44 and those for discs in the same region are constant at about 1.1. This compares with 0.5 used by Nicolson and 0.805 for sandstone and 1.4 for limestone found by Gray in actual measurements. However, in Gray's work, a variety of shapes were used which were neither spherical or disc shaped. His particles were field samples and thus his results are considered more adaptable

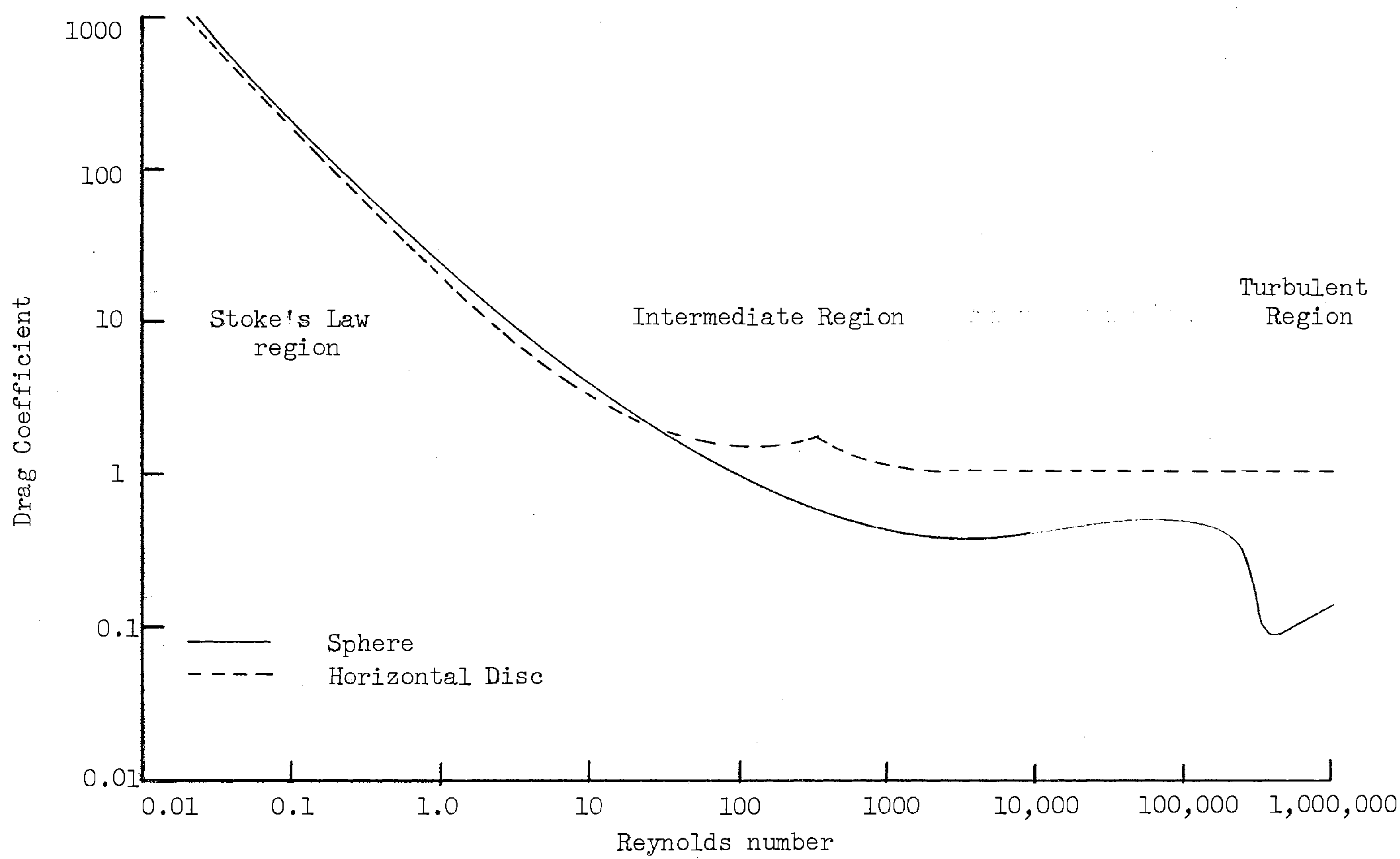


Fig. 4. Drag Coefficient for Spheres, Discs and Cylinders

From Lapple and Shepherd(9)

to gas drilling than those shown in Fig. 4. This range of drag coefficients does show that data accumulated by Gray compares favorably with the investigations made in other fields.

A continuing problem in the determination of drag coefficients is the shape of the particle. A recent review of shape factors was presented by Torobin and Gauvin (10). They reviewed the various shape factors proposed in literature and concluded that only the sphericity factor introduced by Brown has any application in the intermediate flow range.

Pettyjohn and Christiansen (11) in a study of the free fall rates of isometric particles, concluded that the shape deviation could be adjusted using the concept of sphericity. They defined their drag coefficient as shown in Equation (15).

$$C_D = 5.31 - 4.88x \tag{15}$$

It should be noted that using $x = 1$ for a sphere, the drag coefficient is 0.43 which agrees with Lapple and Shepherd. In using Equation (15) the investigators suggested the use of equivalent diameter for the particle diameter.

The equivalent diameter is defined by Equation (16).

$$D_e = \left[\frac{6 \ V_p}{\pi}\right]^{1/3} \tag{16}$$

In Equation (16), D_e reduces to D_p if a sphere is used. Because of the difficulty in correlating the variety of different particle shapes in the study of drag coefficients, a large quantity of the work for irregular shaped particles had been completely empirical in nature.

Burke and Plummer (12) made a study by suspending coke particles in a turbulent air stream and obtained the slip velocity relationship shown in Equation (17).

$$v_s = 13.8 \left[\frac{D_e \, (\rho_p - \rho_f)}{\rho_f} \right]^{1/2} \qquad (17)$$

They used a drag coefficient of 2.25 in Equation (17). Martin (13) obtained a drag coefficient of 1.98 for quartz grains suspended in an air stream.

Miller and McInally (14) found that for coal, anthracite, sandstone and pyrite particles the drag coefficient was 1.3. For shale particles they found the drag coefficient to be 1.8. In general these results indicate the drag coefficients to be higher than those found by other investigators and suggest that test conditions may contribute substantially to the magnitude of drag coefficients. Many conclusions have been made relative to drag coefficients for particles of various shapes and densities. In many cases, there is a wide disagreement on drag coefficients. A look at testing procedures shows that drag coefficients have been determined using a variety of different testing techniques.

In many tests the particles have been dropped through quiescent fluid streams. In others they have been suspended in transparent flow systems, where air flow rate is then equal to particle slip velocity. They have been studied as single particles in large and small flow streams or as a mass of particles in large and small flow streams. With this variety of testing procedures many variables have been ignored.

Barker (15) in a study of work by other investigators concluded that particle density has an effect on drag coefficients other than that accounted for by the use of particle Reynolds number. For cylindrical particles in the intermediate flow range, he showed a decrease in drag coefficients with an increase in particle-fluid density ratio.

A composite look at the literature indicates that for gas-particle systems in the dilute range, (less than four percent solids by volume) the slip velocity is affected to a negligible degree by the solids feed rate. This same type investigation shows a wide variation in conclusions relative to the effect of gas stream velocity.

Lewis, Gilliland, and Bauer (16) using glass spheres in air concluded that the slip velocity was independent of the solids mass feed rate and gas velocity. Their experiments were conducted with 0.0016, 0.0040 and 0.0012 inch diameter spheres in tubes of 2.5 and 4.5 inches in diameter.

Harris and Molstad (17) in the lifting of sand with air concluded that the slip velocity was independent of solids loading. However, they also concluded from their work that the terminal velocity of most solids was about half the gas velocity. This indicates that slip velocity is a function of gas velocity. Their experiments were conducted in 0.267 and 0.532 inch tubes with particles of various types ranging from 0.0036 to 0.00165 inches in diameter.

Culgan (18) using tenite and alundum particles, soybeans, and cottonseed in a three inch diameter pipe concluded that slip velocity is independent of the solids feed rate, but a direct function of gas velocity. His particles ranged in size from 0.03 to 0.33 inches in diameter.

Huntington and Williams (20) in a recent report showed that gas velocity affected slip velocity. In fact they showed no particle slip at a gas velocity of 3000 ft/min. They were using a 20-foot flow column where the solids entered the column at a velocity higher than the terminal particle velocity. Based on their results it is suspected that the particles never reached a terminal velocity through the entire length of the flow column. They concluded that their tests proved that a standard air velocity of 3000 ft/min was sufficient in most gas drilling.

The wide range of disagreement among investigators relative to the effect of gas velocity on slip velocity seems to indicate that test conditions varied a considerable amount. From a review of all the work it is not clear in many cases whether acceleration of the feed solids was considered. Relative to this problem, Russ (21) showed that pressure drop per foot of length for gas flow does not become constant for a distance of 14 feet from the entrance. This indicates that the gas has not reached a steady flow condition for this distance. Culgan reported that the conduit length to allow particles to reach a constant velocity may be appreciable. Hinkle (22) in a study using horizontal pipe showed a length of 30 feet was necessary to reach a constant velocity. Obviously the distance required for a solid particle to reach a point of constant velocity will depend on the velocity of the particle and gas entering the flow column, the size of the flow column, and whether the flow column is vertical or horizontal.

One reasonable answer to these wide variations in results could be explained by the fact that the drag coefficient is a function of the fluid Reynolds number as well as the particle Reynolds number. It has been a common engineering practice to assume that the drag coefficient versus particle Reynolds number curves obtained by the fall of solids through a quiescent fluid could also be used for a dynamic fluid system. Zenz (23) in a correlation of drag coefficients versus particle Reynolds number also used fluid Reynolds number as a parameter. His results are shown in Fig. 5 and indicate that the drag coefficient at least in the range studied is a function of fluid Reynolds number. These data were not discussed by Zenz. However, the data indicate that drag coefficients are a function of fluid Reynolds number and that the use of static systems for the determination of drag coefficients is not reliable. This study lends support to the manner in which Gray determined drag coefficients since his study was conducted in the range of fluid Reynolds numbers used in field practice.

Data from the literature emphasizes that the slip velocity is independent of the solids feed rate, as long as the fluid solids system is in the dilute range. When the concentration of solids exceed this range, concentration affects slip velocity and solids removal. To illustrate the effect of solids concentration, Fig. 6 which is similar to curves from many literature sources, has been prepared, which is the normal pattern of pressure drag versus mass flow rate.

An explanation for Fig. 6 can be made starting at point A. Decreasing the gas velocity results in an increase in solids concentration but a net decrease in pressure loss between points A-B, because of the lower gas velocity. At point B the concentration of solids begins to increase rapidly until finally at point C, the gas velocity can no longer remove the solids. At this point the solids collapse in the tube and the flow chamber is choked-off. The velocity at point C is called the choking velocity.

In general investigators have considered this choking velocity to be greater than the terminal velocity of the solids in the normal fluid system. Several explanations have been given for the solids

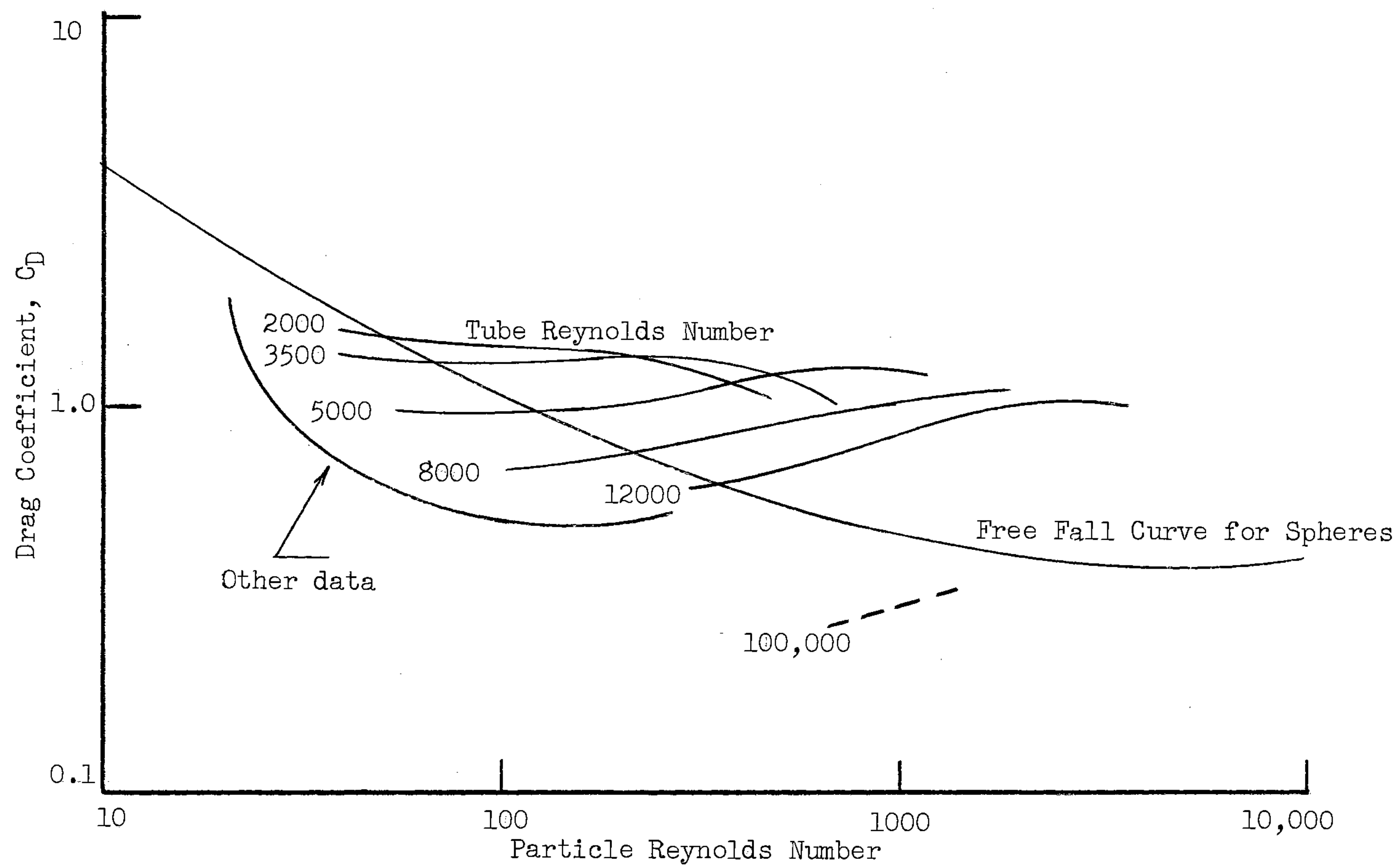


Fig. 5. Drag Coefficient vs Reynolds Number Based on Balancing Velocities

From Zenz (23)

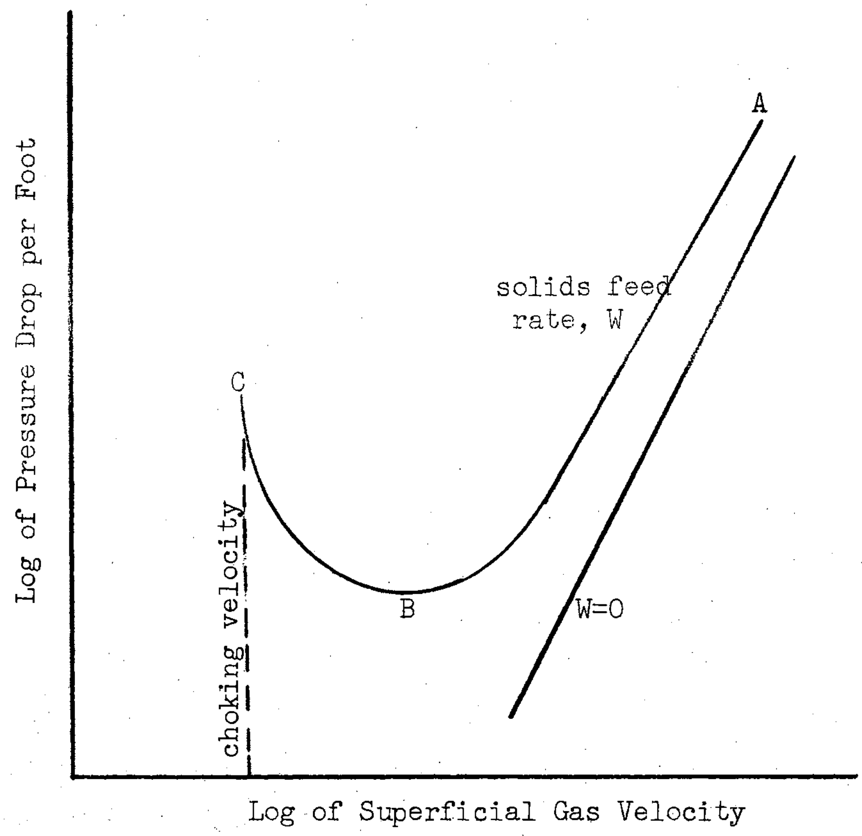


Fig. 6. Pressure Loss vs Gas Velocity

From Wilhelm (27)

collapse at point C. Zenz gave a reasonable explanation and this is summarized as follows:

A particle in a turbulent fluid stream leaves behind a turbulent wake. Since induced turbulence reduces the drag coefficient, any particle entering this wake will require a higher gas velocity for suspension. With no increase in gas velocity, the particle falls and contacts the particle below it. These two particles provide for a considerable increase in mass with very little increase in lift area. Thus the fall rate of the particles is increased. In a very short time, this process of particle accumulation accelerates and the sudden increase in concentration causes a collapse of all the solids.

Zenz, recognizing the importance of concentration and the choking velocity, suggested the empirical formula shown as Equation (18).

$$W = \rho_p (1 - \varepsilon)(v_c - v_s) \tag{18}$$

Equation (18) assumes the particles will have some velocity between the choking velocity and the particle slip velocity. Applying this equation to experimental data, he found values for voidage, ε, ranging from 0.943 to 0.984.

Dallavalla (24) proposed Equation (19) for estimating the choking velocity in vertical pneumatic transport lines.

$$v_c = \left[\frac{910\,\rho_p}{p + 62.3}\right] D_p^{0.6} \tag{19}$$

Dallavalla's equation is based on particles 4-6 and 14-20 mesh with a particle density of 187 lbm/ft^3 and less. Also his experiments were conducted at very low solids concentrations. However, his equation does give a basis for comparing the choking velocity in his system with the assumed velocity of 3000 ft/min of standard air used in the oil industry for normal gas drilling. This is shown in Example 1.

Example 1.

Assumptions: $\rho_p = 187 \text{ lbm/ft}^3$

$p = 14.7$ psia

$D_p = 0.1$ inch

$$v_c = \left[\frac{(910)\ (187)}{14.7 + 62.3}\right]\frac{0.1}{12} = 17.75 \text{ ft/sec} = 1065 \text{ ft/min}$$

Example 1 shows that the choking velocity is about one-third of the normal velocity used in gas drilling. This means that under normal methods of design that the velocity required for lift of formation particles is substantially above the choking velocity, at least as described by Dallavalla.

Zenz and Othmer (25) included a table of recommended lift velocities for various types of materials. These data are shown in Table I and it is noted that recommended velocities are higher than those considered necessary in gas drilling.

Zenz and Othmer proposed a correlation between the group $v_c^2/g\ D_p \rho_p^2$ and the dimensionless group formed by the ratio of solids to gas ratio $W/v_s \rho_f$. The velocity term, v_c, represents the choking velocity and correlates with the same choking velocity term discussed by Dallavalla and shown in Example 1. Using experimental data Zenz and Othmer obtained Fig. 7 for uniform particles and Fig. 8 for mixed particles sizes. These data present an interesting concept and offer a basis of further consideration.

TABLE I

VELOCITIES RECOMMENDED FOR PNEUMATIC CONVEYING OF VARIOUS MATERIALS (25)

Material	Ft/Sec
Barley	80
Coal, Powdered	65
Coffee Beans	40
Cork, Ground	55
Corn	80
Cotton Seed	65
Feathers, Chicken	10
Flour	55
Hemp	75
Jute	75
Lime	85
Metal Turnings	85
Oats	75
Portland Cement	100
Pulp Chips	75
Rye	80
Salt	80
Sand	100
Sawdust	65
Sugar	80
Wheat	80
Wood Flour	65
Wool	75

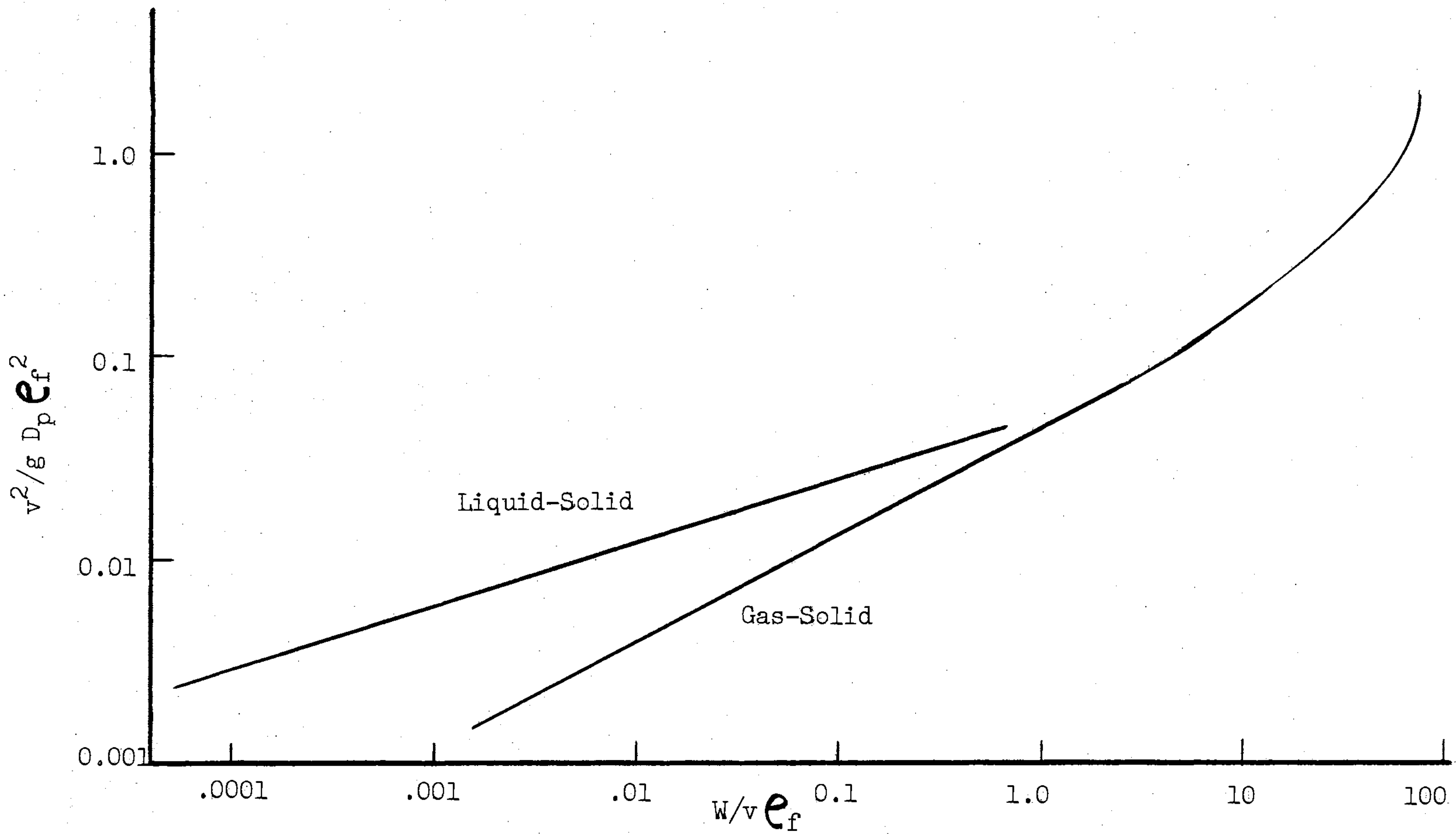


Fig. 7. Choking Velocity Correlations

From Zenz and Othmer (25)

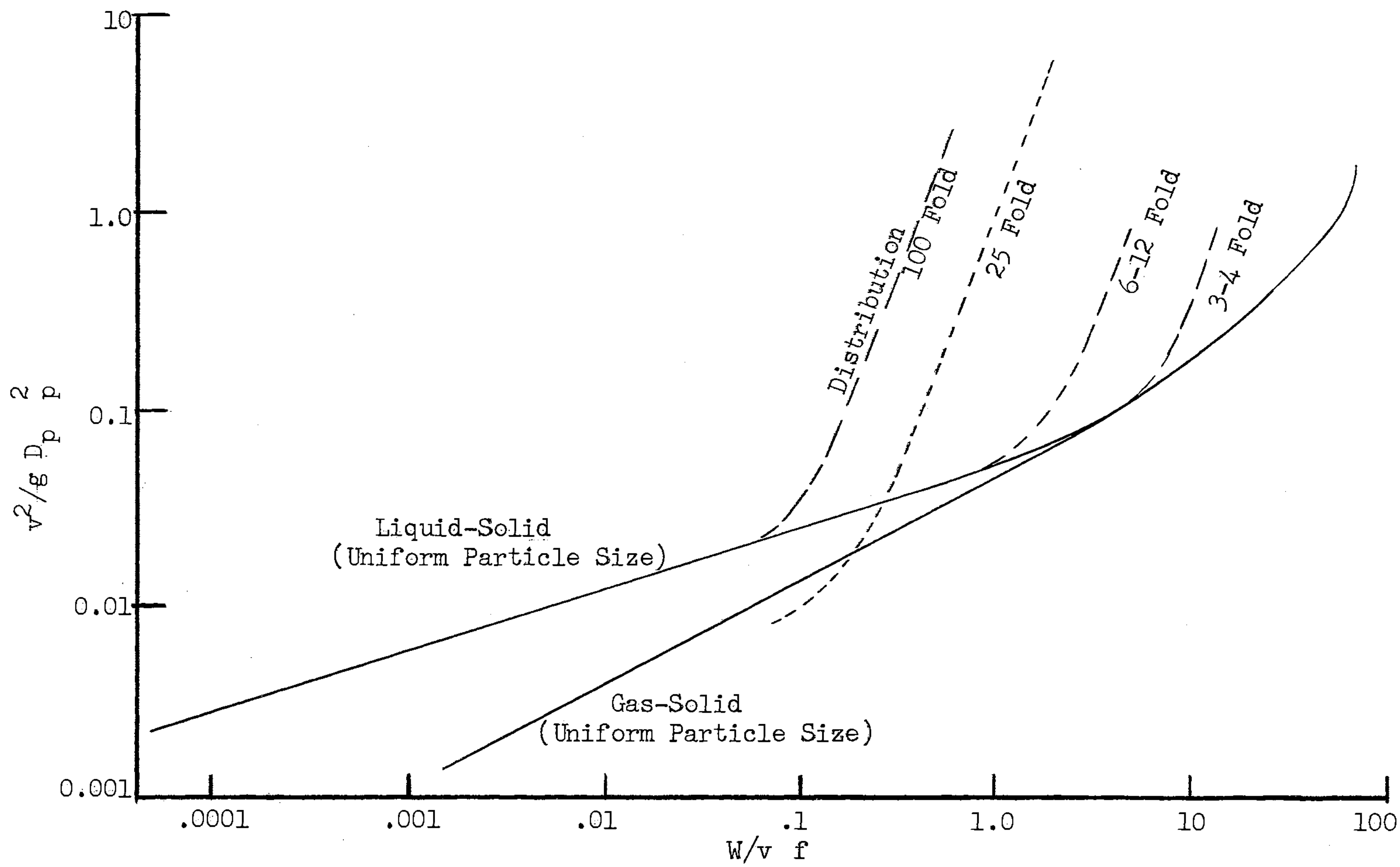


Fig. 8. Choking Velocity Correlations

From Zenz and Othmer (25)

Actually the choking velocity under normal conditions will be less than the required velocity to remove formation cuttings. However it is a limiting factor and the condition may arise where it would be necessary to consider this limitation in the design for gas volume requirements. Conditions where choking velocity might need to be considered are: (1) in a large hole where penetration rates are extremely high, (2) in areas where formation chips are large and tend to accumulate at the top of the drill collars, and (3) situations where the supply of gas is limited. In practice if the supply of gas is not in excess of that required to exceed the choking velocity then either gas drilling should not be attempted or consideration should be given to reducing hole size.

Other past work which contributes to the area of gas drilling is the work performed by various investigators in the determination of pressure losses with two phase mixtures. The problem is an old one as evidenced by the fact Lescher (26), a German mining engineer, described the simultaneous flow of gas and liquid up a vertical tube as early as 1797. A substantial amount of work in this field was performed before 1930, but it was mostly directed towards gas-liquid mixtures and for liquid rates much higher than being considered in this work.

Wilhelm (27) presented a log-log graph of pressure loss versus superficial gas velocity in 1948. The results of his work are shown in Fig. 9. From A to B the solids are stationary and the air blows through. At point B some of the solids begin to move and from C to C' the solids are in a slugging region. Point C has frequently been called the choking velocity point. This represents the point at which solids cannot be lifted because of excessive concentration. In gas drilling

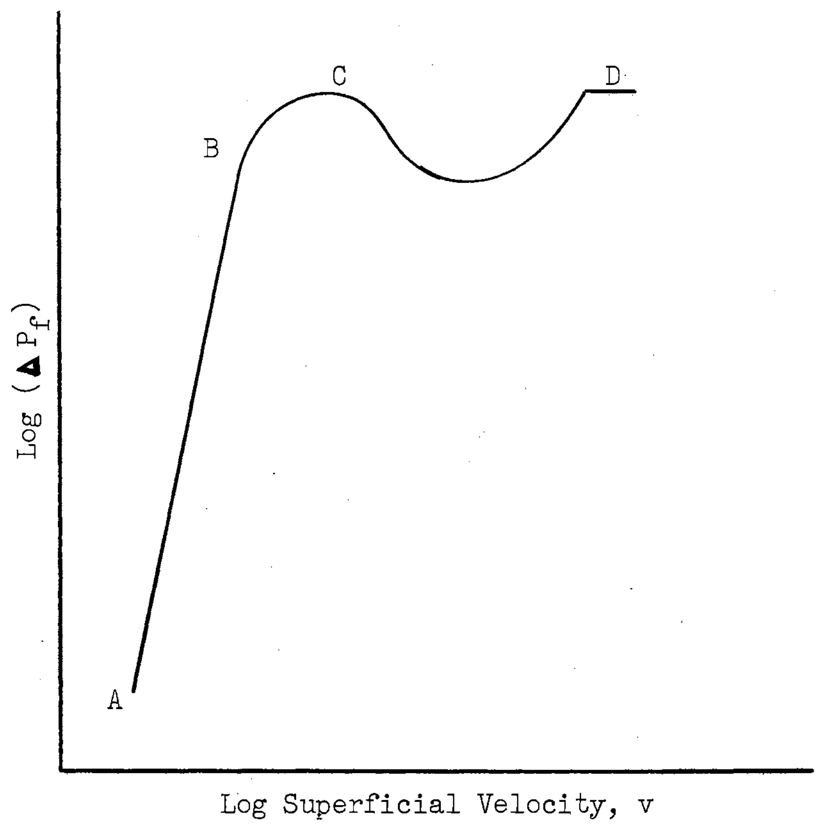


Fig. 9. Pressure Loss vs Gas Velocity

From Wilhelm (27)

only the portion of the curve from C' to D is of general interest. This is the so-called dilute region, the section of the curve where the solids are moving up the vertical column continuously.

Baker (28) in 1963 studied the two phase flow of liquid and air. He made his particular data fit experimental data by using the feed rate of water as a parameter and modifying the Fanning friction factor. These data are not considered applicable in this work. Williams (29) studied the pressure losses experienced by the simultaneous flow of sand and air and his results are shown in Fig. 10. He made no proposal for theoretical calculations.

A recent study of pressure drops around spherical particles was presented by Wentz and Thodos (30). They ran tests for gas pressure losses around spheres that were held stationary. Thus the only pressure loss considered was that due to air-particle friction. The primary purpose of their work was to determine a friction factor or drag coefficient between air and the particles.

Pressure loss as due to friction around the spheres is shown in Equation (20).

$$P = \frac{6 \rho_f v^2 h C_D (1 - \varepsilon)}{2 g_c Dp} \qquad (20)$$

In this equation the voidage or that part of the flow channel that does not contain solids is called ε. In Fig. 11, Wentz and Thodos, show a variable drag coefficient; however, their range of data is substantially below that which is experienced in normal gas drilling operations. Their main contribution to the work in this research was the manner in which pressure loss due to particles was determined.

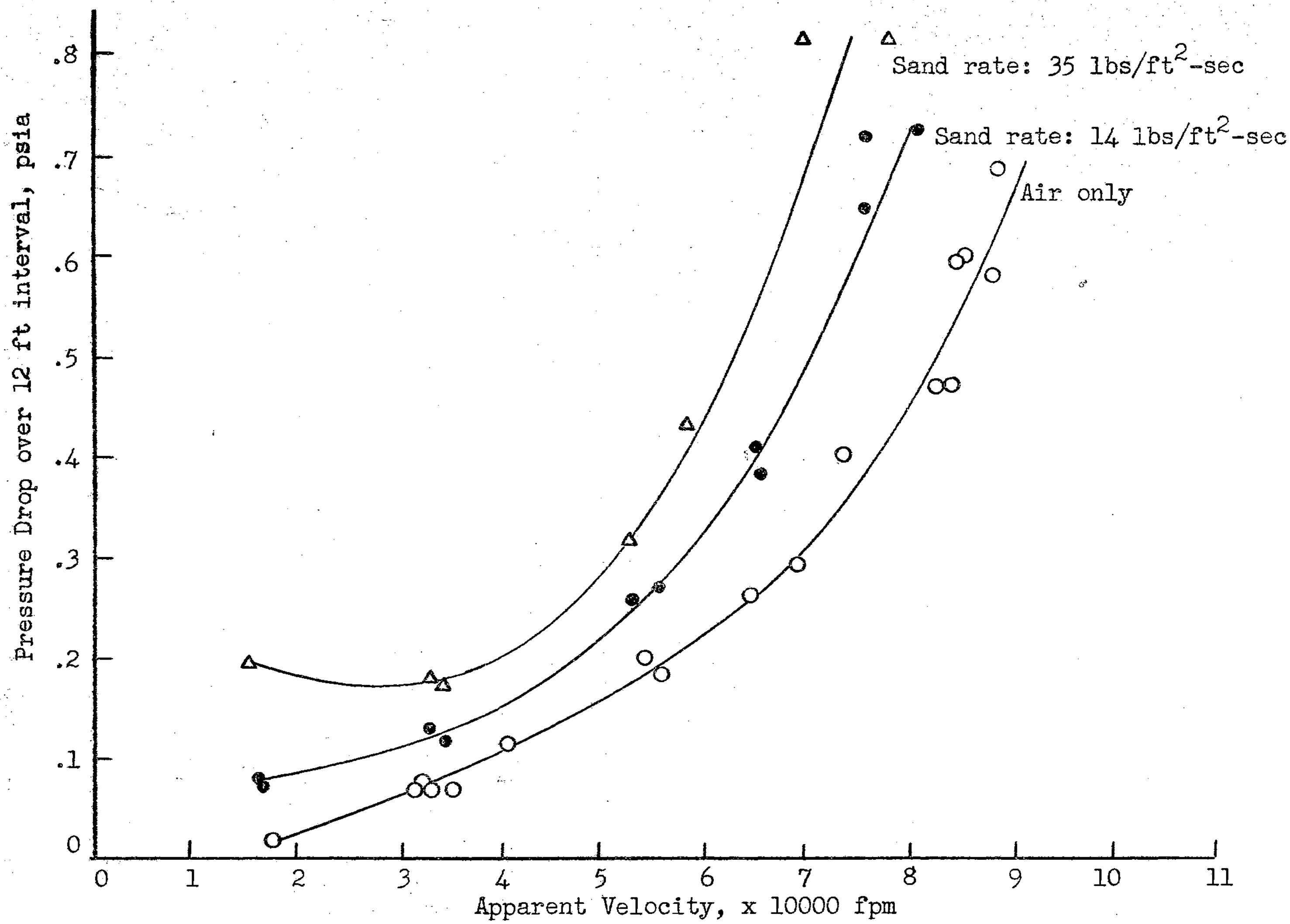


Fig. 10 Pressure Loss vs Flow of Sand and Air
From Williams(29)

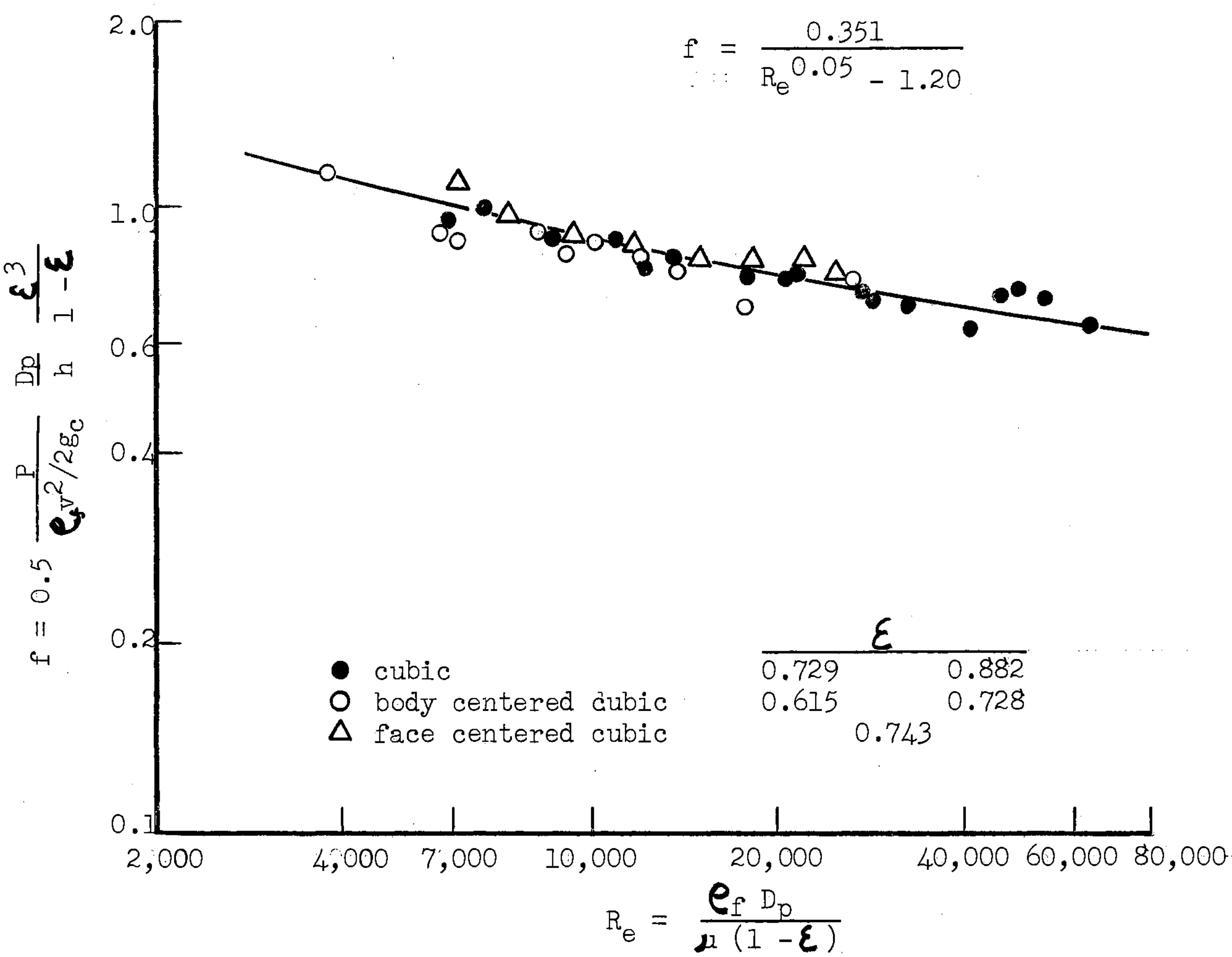


Fig. 11. Drag Coefficient vs Modified Reynolds Number

From Wentz and Thodos (30)

One important problem concerned with cutting size has been investigated by two different organizations. In full scale laboratory drilling studies at the Jersey Production Research Company it was found that 11 percent of the cuttings just above the bit in air drilling are larger than 0.2 inches and 22 percent are larger than 0.1 inches in size. Later, Bruce, Simons, and Whitaker (31) conducted a series of tests to determine cutting size in actual field air drilling operations. Their results are shown in Table II. The results show that about 10 percent of the cuttings at the top of the drill collars are larger than 0.0787 inches in size. However at a distance of 300 feet above the collars only about 7 percent of the cuttings are larger than 0.0787 inches in size. At the surface most of the cuttings had been reduced to powder form. They concluded that the erosive action of the drill pipe was the primary reason for the reduction of cutting size to the powder form observed at the surface.

Other data on solids transportation and pressure losses for two phase mixtures are available; however, this review has been dedicated to presenting information of interest in gas drilling. The following conclusions are made from the summary of previous work.

(1) The methods used to determine volume requirements in rotary gas drilling are not correct mathematically, as a result the oil industry has been forced to rely on trial and error methods of design.

(2) A large quantity of data are available on drag coefficients, but the data presented by Gray are the most accurate for use in gas drilling.

(3) The solids feed rate will not affect the solids slip

TABLE II

SIZE ANALYSIS OF JUNK BASKET SAMPLES

Location Of Junk Basket	Well Depth ft.	Total Sample Weight Including Mud gm.	Weight Cuttings Retained No. 6 Screen gm.	Weight Cuttings Retained No. 10 Screen gm.	Percent Cuttings Retained No. 6 Screen	Weight Cuttings Retained No. 10 Screen
Above Bit	2984	817.0	15.1	16.2	1.8	2.0
Above Collars	2984	932.5	93.4	26.5	10.0	2.8
303 Ft. Above Collars	2984	723.5	35.4	13.6	4.9	1.9
Above Bit	7533	752.4	160.9	34.0	21.3	4.5
Above Collars	7533	752.0	33.5	36.1	4.4	4.8
1,000 Ft. Above Collars	7533	863.8	33.3	43.1	3.9	5.0

velocity as long as the concentration of solids is the dilute range (less than 4 percent solids by volume).

(4) Choking velocity should be considered in design criteria as a limitation on minimum allowable gas volumes.

(5) In calculating pressure loss for gas-solids systems, pressure drop due to air-particle friction must be considered separately and the assumption that solids go into solution is not valid.

(6) Cutting sizes at the top of the drill collars will in general be 0.2 inches or less in size.

CHAPTER III

THEORETICAL CONSIDERATIONS

The theoretical considerations will be directed towards the development of mathematical models that can be used to predict the gas volumes required to remove solid particles in rotary gas drilling. As shown in the summary of previous work the calculation of gas volumes has been attempted by other investigators; however, they have made unnecessary assumptions and in some cases used incorrect mathematical models.

Mathematical developments presented in this chapter can be used to calculate two of the more necessary variables in gas drilling. One will be the development of a differential pressure equation that can be used to predict the pressure at any point in a vertical hole for a flowing mixture of gas and solids. The other will be the equation for predicting the mass of gas required to lift given size solids at the predicted pressure. This pattern of development is similar to that taken by other investigators; however, a completely new differential pressure equation for a gas-solids mixture will be developed. Also as a supplement to this equation it will be shown that only the most difficult point of lift which is just above the drill collars should be considered for vertical flow. Therefore the program design for solids lift will be at one point and not for all points in the vertical flow column. In conjunction with this development the effect of gas and solid deacceleration will be shown and discussed.

The equation for the mass of gas required for particle lift will be based on the common force balance relationships on one solid particle. To account for particle shape, the force balance equation will be developed as if the particle being lifted is a sphere and the deviation in particle shape will be considered in the empirical drag coefficients. From this equation the actual mass of gas required for solids lift will be considered. The assumption of a standard gas velocity of 3,000 ft/min, which was necessary in previous work, is eliminated in this work.

In addition to the mathematical model developed using the differential pressure force balance equations, the effect of excessive solids loading will be considered. Past references indicate the solids feed rate has a negligible effect on the mass of gas required to lift particles until the concentration of solids by volume reaches from three to four percent of the gas volume. At this point solids choking can occur with the result that the solids are removed in slugs or lift may cease completely. This effect will be considered, but only as a design limitation. However, even in this category it is an important factor even though it has received no attention in previous investigations of gas volume requirements.

Location of Most Difficult Point of Lift

In the design of gas volume requirements, the point of design should be where the greatest mass of gas is required to lift a given size particle. In past work investigators have used a geothermal gradient or average temperature and determined the support capacity of the gas in the entire annulus.

The point of most difficult lift will be shown using the slip velocity equation for spherical particles. This equation is developed completely in Appendix A. The slip velocity for one particle may be calculated using Equation (21).

$$v_s = \left[\frac{4\ g\ D_p\ \rho_p}{3\ C_D\ \rho_f}\right]^{1/2} \qquad (21)$$

If it is assumed that the slip velocity is equal to the upward gas velocity the particle floats in the gas stream. Any small increase in gas velocity will lift the particle, thus the assumption will be used that $v \approx v_s$. This will be true when the velocity of the particle approaches zero. Using this assumption in Equation (21) and multiplying both sides of the equation by $\rho_f A$, results in Equation (22).

$$G = A\left[\frac{4\ g\ D_p\ \rho_p\ \rho_f}{3\ C_D}\right]^{1/2} \qquad (22)$$

where: $G = \rho_f\ Av$, the mass of flowing gas.

Equation (22) can be further modified by expressing density in terms of pressure and temperature as shown in Equation (23).

$$G = A\left[\frac{4\ g\ D_p\ \rho_p\ SP}{3\ C_D\ 53.3\ T}\right]^{1/2} \qquad (23)$$

From Equation (23), it can be seen that the mass of gas required to lift a given size particle is a function of the drag coefficient, flow channel area, and the pressure and temperature of the gas. In the flow region using gas, the drag coefficient is considered a constant and for design purposes the flow channel size is also considered constant. Using the concept of an average temperature or the temperature at a given point, the mass of gas required to lift a given size particle increases as the pressure increases. The point of highest pressure would be at the bottom of the hole; however the lift area between the drill collars and the hole is about one-half the area between the drill pipe and hole. The smaller area reduces the mass of gas required for lift around the drill collars even though the pressure is higher. In all cases, the higher pressure at the bottom of the hole will not be high enough to offset the differences imposed by the smaller lift area. The point of most difficult lift will be located at the top of the drill collars, where the lift area is a maximum and the pressure is at its highest level for this same flow area.

Bruce, Simons and Whittaker in their investigation found that particle sizes in gas drilling became smaller as the solids moved up the annulus. They concluded that the smaller size was a result of erosion due to collisions between solids and the drill pipe. An important reason for the smaller solid size which they apparently overlooked was the fact that the gas volume may have been too low to lift the larger particles. Thus these particles remain in a relatively short zone at the top of the collars. If the rate of influx of large cuttings into this zone exceeds the rate at which they are broken up and removed, the accumulation will result in particle slugging, and eventually the choking-off of solids lift.

Zone of Solids Accumulation at the Top of the Drill Collars

The zone of solids accumulation just above the drill collars could have a serious effect on normal gas drilling operations. It is the purpose of this development to predetermine the length of this zone. In many of the laboratory experiments, several investigators predicted the distance required for the gas or solid to reach a terminal velocity. In the annulus above the drill collars the distance required to expend the kinetic energy imparted to the gas and solids due to higher velocities around the drill collars, can be obtained by a reduced form of the energy balance. For this purpose refer to Fig. 12, which is a schematic diagram of the portion of the annulus to be considered. Point A, at the top of the drill collars represents the entrance to the system. Assuming a steady flow of gas the general energy balance reduces to Equation (24).

$$\overline{KE}_i - \overline{KE}_o + \overline{PE}_i - \overline{PE}_o + \overline{H}_i - \overline{H}_o + \frac{\Sigma Q}{M} - \frac{\Sigma W}{M} = 0 \qquad (24)$$

Because of the short distance of observation, both the change in enthalpy and heat transfer effects are considered negligible. Also the selection of an open system with fixed boundaries means the work term equals zero. Thus Equation (24) reduces to Equation (25).

$$\overline{KE}_i - \overline{KE}_o + \overline{PE}_i - \overline{PE}_o = 0 \qquad (25)$$

Inserting the definitions of potential and kinetic energy results in Equation (26).

$$\frac{Mv_i^2}{2g_c} - \frac{Mv_o^2}{2g_c} = M \frac{g}{g_c} h \qquad (26)$$

Solving Equation (26) for h gives Equation (27).

$$h = \frac{v_i^2 - v_o^2}{2g} \qquad (27)$$

An Example, B-1, showing the use of Equation (27) is included in Appendix B.

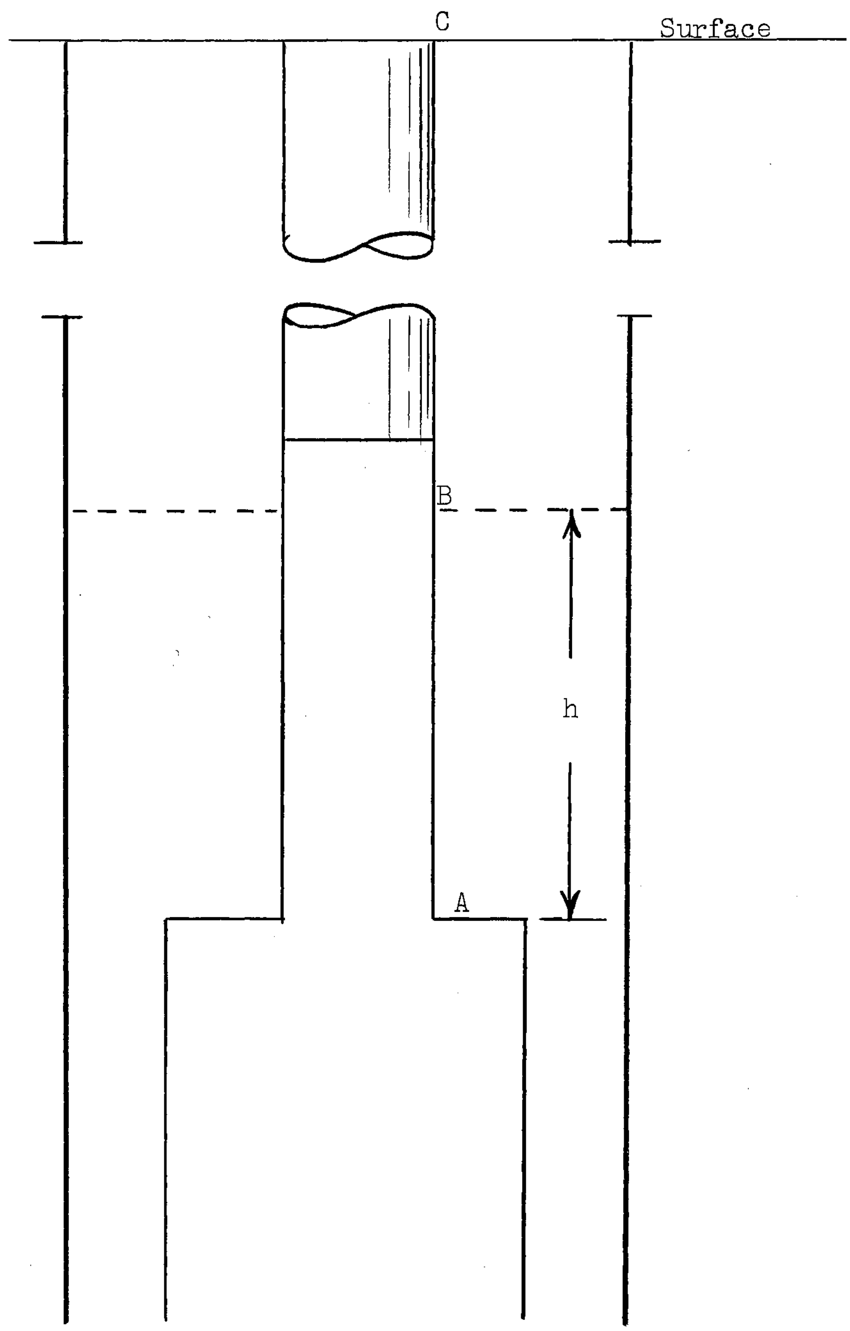


Fig. 12. Diagram of Drill String-Hole Annulus

The length of the zone of solids accumulation may be important. A short interval of solids accumulation may magnify the problem because less tolerance is permitted between the entry of solids too large to lift and the rate at which they must be broken up so they can be removed from the hole.

After establishing the fact that the primary problem of lift in gas drilling is just above the drill collars, the next problem is an equation that will predict the pressure loss in this region of the annulus. The losses in energy can be described by the use of the general energy balance for both the gas stream and particles. Again a steady state system will be chosen such that the entrance to the system is above the drill collars at Point B in Fig. 12 and the system outlet is at Point C at the surface. Point C is actually the point at which the gas and solids discharge to the atmosphere. Using this approach and this system the general energy balance reduces to that shown in Equation (28).

$$\bar{H}_i - \bar{H}_o + \overline{PE}_i - \overline{PE}_o + \overline{KE}_i - \overline{KE}_o + \frac{\delta Q}{M} - \frac{\delta W}{M} = 0 \qquad (28)$$

Again the open system with fixed boundaries gives a $\delta W = 0$. However, in this case other changes in energy must be considered since the entire annulus is being considered rather than a short section. The heat transfer term may be defined by the entropy balance as shown in Equation (29).

$$\frac{\delta Q}{M} = T\Delta\bar{S} - T\bar{S}_p \qquad (29)$$

The heat transfer term in Equation (28) may be written as shown in Equation (30).

$$\frac{\delta Q}{M} = d\bar{H} + d(\overline{PE}) + d(\overline{KE}) \qquad (30)$$

Using the definition that, $d\bar{h} = Td\bar{S} + \bar{V}dP$, which can be developed by simple thermodynamic applications and Equation (29) for δQ results in Equation (31).

$$\bar{V}dP = d(\overline{PE}) + d(\overline{KE}) + T\bar{S}_p \qquad (31)$$

Using density instead of specific volume and substituting the definitions of $d(\overline{PE})$ and $d(\overline{KE})$, Equation (31) reduces to what is commonly called the Bernoulli equation as shown in Equation (32).

$$dP = \frac{\rho g}{g_c} dh + \frac{\rho v \, dv}{g_c} + \rho T\bar{S}_p \qquad (32)$$

The $T\bar{S}_p$ term represents what is commonly called the irreversible work term. Actually it is the energy lost in overcoming frictional resistance. The complete term $\rho T\bar{S}_p$ is the pressure required to overcome the frictional resistance. The pressure required to overcome friction may also be expressed using empirical friction factors.

The Moody friction factor, a commonly used empirical factor, is defined as shown in Equation (33).

$$f = \frac{2 P D g_c}{\rho v^2 h} \qquad (33)$$

Rearranging this equation and expressing the differential pressure as a function of the differential height in the annulus gives Equation (34), which is the expression for the pressure losses necessary to overcome friction.

$$dP = \frac{\rho v^2 f \, dh}{2 g_c D} \qquad (34)$$

Using Equation (34) in Equation (32) gives Equation (35).

$$dP = \frac{\rho g}{g_c} dh + \frac{\rho v \, dv}{g_c} + \frac{\rho v^2 f \, dh}{2 \, g_c \, (D_h - D_d)} \tag{35}$$

The pressure losses due to changes in kinetic energy, given by the second term on the right hand side of Equation (35), are generally considered negligible relative to the other pressure loss terms. An example, B-2, showing the relative effect of kinetic energy is included in Appendix B.

Assuming that the pressure losses due to changes in kinetic energy are negligible and using the fact that the numerical values of gravity and the units conversion constant g_c are equal results in the commonly used equation for pressure loss using a specific material which is shown as Equation (36).

$$dP = \rho \, dh + \frac{\rho v^2 f \, dh}{2 \, g_c \, (D_h - D_d)} \tag{36}$$

The development for this equation has been presented to illustrate that it is applicable regardless of the flowing material providing the correct interpretation is made for all of the terms. First consider only the pressure losses due to the flowing gas stream. For the pressure loss for gas alone in the annulus, Equation (36) is expressed as shown in Equation (37).

$$dP = \rho_f \, dh + \frac{\rho_f v^2 f \, dh}{2 \, g_c \, (D_h - D_d)} \tag{37}$$

Equation (37) does not account for any loss in pressure due to particles and cannot be modified to account for this particle pressure loss by simply adding a ratio of the mass of particles to the mass of gas as many other investigators have done.

The total pressure loss due to the particles may be expressed as shown in Equation (38).

$$\text{Particle Pressure Loss} = P_{p-p} + P_{a-p} + P_{p-w} \tag{38}$$

where:

P_{p-p} = particle-particle pressure loss

P_{a-p} = air-particle pressure loss

P_{p-w} = particle-wall pressure loss

In normal gas drilling it is assumed that the particle concentration is always in the dilute range. In fact in most cases this concentration of solids will be less than one percent by volume of gas.

As a result, the particle-particle and particle-wall pressure losses are considered negligible. This appears to be an acceptable assumption, when it is realized that the rate of particle slip in the gas may exceed 2,000 ft/min, thus creating a relatively large loss in pressure due to shear as compared with the occasional collision of particles in a dilute gas-solids stream. Using this basis, Equation (36) may be modified to account for the pressure loss due to solids only as shown in Equation (39).

$$dP_p = \frac{M_p \, dh}{V_a} + \frac{F_D}{A_a} \tag{39}$$

The first term on the right side of the equal sign in Equation (39) accounts for the pressure imposed by the static head of solids and the second term accounts for the pressure drop caused by shear of the gas stream around the solids. The total pressure loss due to the shear force may be determined by considering first the shear force due to one particle and then counting the total number of particles.

Shear Force Due to One Particle

The shear force due to one particle may be determined by use of the dimensionless drag coefficient. The drag coefficient considering the particle shape to be a sphere is given by Equation (40).

$$C_D = \frac{2\ P\ g_c}{\rho_f\ v_s^2} \tag{40}$$

Solving Equation (40) for pressure and multiplying each side of the resulting equation by the total particle shear area, A_s, results in Equation (41), which gives the shear force on one particle.

$$F_D = PA_s = \frac{\rho_f\ A_s\ v_s^2\ C_D}{2\ g_c} \tag{41}$$

To use this relationship in Equation (39), it will be necessary to count the total number of particles as shown in Equation (42).

$$N = \frac{A_a\ (1 - \varepsilon)\ dh}{V_p} \tag{42}$$

In Equation (42), $A_a dh$ represents the total volume of the annulus for any differential height and, ε , represents the fraction of the annulus that contains no solids or the voidage. Thus, $1 - \varepsilon$, is that part of the annulus volume that contains solids. The numerator of the right hand side of Equation (42) gives the total volume of solids and dividing by the average volume of each solid the total number of solids is determined. From this the shear force for one solid in Equation (41) may be modified to account for all of the solids by multiplying by the number of solids given by Equation (42). The total shear force for all of the solids is given by Equation (43).

$$F_D = \frac{\rho_f A_s v_s^2 C_D A_a (1 - \epsilon) dh}{2g_c V_p} \tag{43}$$

The differential pressure drop in the annulus due to shear around the solids is given by Equation (44).

$$dP_{a-p} = \frac{F_D}{A_a} = \frac{\rho_f A_s v_s^2 C_D (1 - \epsilon) dh}{2g_c V_p} \tag{44}$$

Equation (44) may be further modified by substituting for the shear area A_s and the volume of the solid V_p. For this purpose, $A_s = \pi D_p^2$ and $V_p = \frac{\pi D_p^3}{6}$, based on the assumption that the solid is a sphere. Also the value of the slip velocity, v_s, as shown in Equation (21) may be used. Using these substitutions Equation (44) may be reduced to that shown as Equation (45).

$$dP_{a-p} = 4 \rho_p (1 - \epsilon) dh \tag{45}$$

The form shown in Equation (45) can be used only by replacing the voidage fraction by some measurable quantity. This is done as follows:

The fractional part of the annulus $(1 - \epsilon)$ occupied by solids can be expressed as shown in Equation (46).

$$1 - \epsilon = \frac{V_s}{V_f + V_s} \tag{46}$$

The volume of solids or particles, V_s, and gas, V_f, can be defined in terms of mass as follows:

$$V_s = \frac{M_p}{\rho_p} \qquad \text{and} \qquad V_f = \frac{M_f}{\rho_f}$$

Substituting these relationships into Equation (46) and simplifying gives Equation (47).

$$1 - \varepsilon = \frac{M_p \rho_f}{M_f \rho_p + M_p \rho_f} \tag{47}$$

Equation (47) can be further simplified by assuming that the term $M_p \rho_f$ is negligible as compared with $M_f \rho_p$, the final definition of, $1 - \varepsilon$, is shown in Equation (48).

$$1 - \varepsilon = \frac{M_p \rho_f}{M_f \rho_p} \tag{48}$$

Proof that this simplification is valid is shown by an example B-3 in Appendix B. It is shown that in the actual case differential pressure at any point in the annulus of a rotary drilled well would be affected by less than 0.1 psi by omitting the $M_p \rho_f$ term in the denominator of Equation (47).

Substituting Equation (48) into Equation (45) shows that the air-solid pressure loss can be defined as indicated in Equation (49).

$$dP_{a-p} = \frac{4M_p \rho_f \, dh}{M_f} \tag{49}$$

The total pressure loss due to solids may be written by combining Equations (39) and (49). This result is shown as Equation (50).

$$dP_p = \frac{M_p}{V_a} dh + \frac{4M_p}{M_f} \rho_f \, dh \tag{50}$$

The M_p/V_a term can be expressed as $\rho_f M_p/M_f$, by the same argument used to develop Equation (48). If this is done Equation (50) for total pressure drop due to solids may be written as shown in Equation (51).

$$dP_p = \frac{\rho_f M_p dh}{M_f} + \frac{4M_p \rho_f dh}{M_f} = \frac{5 \rho_f M_p dh}{M_f} \qquad (51)$$

Equation (51) shows that the pressure loss due to solids is a function of the ratio of solids mass to gas mass times the density of the gas. This relationship for pressure loss due to solids is considerably different than that introduced in the summary of previous work. As noted particle diameter does not appear in the final equation except as it would be used in calculating the total mass of solids.

Because the equations for solids pressure loss and pressure loss due to friction of the flowing gas stream were considered separately, the total pressure drop due to the gas-solids mixture can be obtained by simply adding Equations (37) and (51). The relationship for total differential pressure at any point is shown as Equation (52).

$$dP = \rho_f (1 + \frac{5M_p}{M_f})dh + \frac{\rho_f v_f^2 f dh}{2g_c (D_h - D_d)} \qquad (52).$$

Equation (52) represents an entirely new development for the determination of pressure loss for a flowing stream of gas and solids. It should be noted that Equation (52) is only valid where the concentration of solids is in the dilute range, the normal condition in gas drilling.

The ratio M_p/M_f will be a function of the mass of solids which enter the flowing fluid stream and the mass of fluid required to lift these solids. In terms of measurable quantities this ratio can be expressed as shown in Equation (53).

$$\frac{M_p}{M_f} = \frac{1603\ D_h^2\ r}{S\ Q_s} \tag{53}$$

In Equation (53) the particle specific gravity has been assumed to be 2.5 and the total mass of gas has been expressed in terms of the total mass at standard conditions. The S term is the gas specific gravity relative to the density of air. The gas density at any point is expressed as shown in Equation (54).

$$\rho_f = \frac{S\ P}{53.3\ Z\ T_a} \tag{54}$$

The gas velocity at any point is shown in Equation (55).

$$v = \frac{P_s\ Q_s\ ZT_a}{A\ T_s\ P} = \frac{5.19\ Z\ Q_s\ T_a}{(D_h^2 - D_d^2)\ P} \tag{55}$$

Combining Equations (54) and (55) with Equation (52) gives Equation (56). Equation (53) is not utilized at this point since M_p/M_f is not a function of pressure or well depth.

$$PdP = \frac{SP^2}{53.3\ ZT}\left[1 + 5\,\frac{M_p}{M_f}\right] dh + \frac{7.84(10^{-3})\ SZ^2Q_s^2\ T_a\ f}{(D_h^2 - D_d^2)^2\ (D_h - D_d)}\ dh \tag{56}$$

The detailed solution to Equation (56) is shown in Appendix A. The final solution is shown as Equation (57).

$$P = \left[(P_w^2 + \frac{a}{b})\ e^{2bh} - \frac{a}{b}\right]^{1/2} \tag{57}$$

where:

$$a = \frac{7.84\,(10^{-3})\; S\; Q_s^2\; T_a\; f}{(D_h^2 - D_d^2)^2\;(D_h - D_d)}$$

$$b = \frac{S}{53.3\; T_a}\,(1 + 5\,\frac{M_p}{M_f})$$

The gas deviation factor, Z, has been omitted from the definitions a and b, since at the low pressures encountered in gas drilling the gas behavior will be ideal. If desired, the Z factor can be used. As shown in Equation (57) the pressure loss at any point in the annulus will depend on the volume flow rate of gas, which means that Equation (57) contains two unknowns.

The volume flow rate of gas required to lift a given size particle at a given pressure can be obtained by modifying Equation 23, which defines gas requirements to lift a given size particle in terms of mass. The mass of gas required to lift particles is rewritten, using a particle density of 156 lbm/ft^2, as shown in Equation (58).

$$G = 8.8\,(D_h^2 - D_d^2)\left[\frac{S\; D_p\; P}{C_D\; T}\right]^{1/2} \qquad (58)$$

A detailed development of Equation (58) is in Appendix A. Although Equation (58) was developed from a force balance on a single spherical particle it is assumed that it can also be used for any number of particles, since previous work showed the solids feed rate had practically no effect on the gas volume required to lift solids. Because flow is confined to one direction in the annulus, the one-dimensional continuity equation may be used with Equation (58) to determine the volume of gas in standard cubic feet per second required to lift the cuttings. It should be re-emphasized that the mass of gas required from Equation (58) will be at one point in the annulus and the continuity equation is used only to convert to standard conditions of measurement. The continuity equation is shown as Equation (59).

$$\rho A v = \rho_s A v_{ss} \tag{59}$$

Since, $G = \rho A v$, and, $A v_{ss} = Q_s$, Equation (59) may be rewritten as shown in Equation (60).

$$G = \rho_s Q_s \quad \text{or} \quad G = 0.0764 S Q_s \tag{60}$$

where: 0.0764 is the density of air at standard conditions and

S is the gas specific gravity.

Using Equation (60) with Equation (58) gives Equation (61), which defines the required rate of gas flow as a function of pressure.

$$Q_s = 115.1 (D_h^2 - D_d^2) \left[\frac{D_p P}{S C_D T}\right]^{1/2} \tag{61}$$

Equations (57) and (61) contain two unknowns, the pressure and flow rate of gas and may be solved simultaneously to obtain solutions. This will be done by rearranging Equation (61) to solve for pressure and setting Equations (57) and (61) equal to each other. The result of doing this is shown in Equation (62).

$$\frac{0.752 (10^{-4}) C_D T Q_s^2 S}{(D_h^2 - D_d^2)^2 D_s} = \left[(P_w^2 + \frac{a}{b}) e^{2bh} - \frac{a}{b}\right]^{1/2} \tag{62}$$

Since both a and b contain Q_s and b is contained in the exponent of e, it is easier to solve this equation by trial and error than to attempt a direction solution for Q_s. It should be noted that the solutions to Equation (62) also give the pressure at that depth in pounds per square foot.

Equation (62) is assumed to have application so long as the concentration of solids is less than 4 percent by volume of the total occupied space. Under normal conditions of gas drilling this will not be a problem since in the general case the concentration of solids by volume will be less than one percent. However in special cases where the supply of gas is substantially below that required for normal lift, consideration should be given to concentration effects.

To understand the problem of excess solids concentration refer back to the summary of previous work where the concept of choking velocity was discussed. Zenz showed that the choking velocity occurred between the ratio of solids volume to gas volume of 1.5 to 4.5 percent depending on the operating conditions. Based on Zenz's results, a solids concentration of four percent was considered the maximum permissible before choking would occur. This relationship is shown in Equation (63).

$$\frac{\text{Volume of Solids}}{\text{Volume of Gas}} = 0.04 \qquad (63)$$

This may be expressed in the form of variables used previously expressing the solids volume in terms of hole size and drilling rate and using the required quantity of gas, Q_s, for lift.

The volume of solids will be a function of the hole size and drilling rate and may be expressed as shown in Equation (64).

$$\text{Volume of Solids} = \frac{\pi D_h^2 r}{4} \qquad (64)$$

The volume of gas may be expressed in terms of the total gas mass as shown in Equation (65).

$$\text{Volume of Gas} = \frac{S\,\rho_s\,Q_s}{S\,\rho} = \frac{\rho_s\,Q_s}{\rho} \qquad (65)$$

Using Equations (64) and (65), Equation (63) can be expressed as shown in Equation (66).

$$\frac{0.7854\,D_h^2}{\dfrac{\rho_s\,Q_s}{\rho}} = 0.04 \qquad (66)$$

Solving Equation (66) for Q_s and expressing density in terms of temperature and pressure gives Equation (67).

$$Q_s = \frac{4.825\,D_h^2\,r\,P}{T} \qquad (67)$$

Equation (67) can be solved with less difficulty by trial and error since the pressure term also includes the Q_s term. Equation (57) defines pressure as a function of the volume flow rate and when combined with Equation (67) gives Equation (68) for the determination of the volume requirements necessary to prevent solids choking.

$$\frac{Q_s\,T_a}{4.825\,D_h^2\,r} = \left[\left(P_w^2 + \frac{a}{b}\right)e^{2bh} - \frac{a}{b}\right]^{1/2} \qquad (68)$$

Equation (68) introduces a completely new concept into rotary drilling with gas. This is the minimum permissible gas requirements if gas drilling is to be continued in any given hole. If gas volumes available are less than those stated necessary by Equation (68) solids choking will probably occur.

There are several occasions when the accumulation of solids might reach the point where solids choking could occur. If drilling continues with volumes less than those required from Equation (62), there will be an accumulation of solids above the drill collars. It is possible that gas drilling can continue because the solids will be broken into smaller sizes just above the collars. However the build-up in solids concentration cannot exceed four percent solids by volume or solids choking will occur. Another situation that could result in an accumulation of solids would be an enlarged hole section. Assume for example that the hole size was two times its original size. It may be possible to show that drilling with gas cannot continue, because the volumes available are less than those required for the enlarged hole section.

Summary

Parts of the theoretical investigation of this chapter are similar to previous work. For example the idea for solving simultaneous equations for the determination of Q_s, the volume of required gas, was introduced by Angel. The slip velocity equation is an adaptation of a force balance on one particle, the concept for which, was introduced by Newton's second law, F = ma. The use of this equation with the assumption that the particles were spherical and changing the drag coefficient to account for shape deviations were introduced by Gray.

From this past information, it has been possible to extend old ideas and introduce new ones which will permit the engineer to have more confidence in the future design for gas volume requirements. For example, it can be shown that small variations in pressure can introduce substantial increases in gas volume requirements. Thus it was necessary to improve the method of predicting pressure in the drill pipe-hole annulus. This has been done and the results should offer a substantially more reliable means for predicting pressure loss for a mixture of solids and gas. Even more important it offers the basis for a continued investigation into the simultaneous flow of three phases, where a liquid phase is added.

By using the actual drag coefficients, and designing the gas volume requirements for a specific point in the annulus, the assumptions of 3,000 ft/min for standard air and the conflict in mathematics introduced by the dual use of the force balance equation on a particle and the continuity equation has been eliminated.

Previous work shows concentration effects in normal gas drilling to be negligible. However, the region where trouble might be incurred has been illustrated and an entirely new method of handling such a problem has been introduced. This is the first time concentration effects have been considered in gas drilling and it opens the door to further consideration of the problem when liquid is added as the third phase.

CHAPTER IV

EXPERIMENTAL WORK

Experimental tests were conducted both in the laboratory and at a drilling rig, where air was being used as the circulating fluid. Field tests were conducted primarily to determine the annulus pressure losses while drilling with air. Laboratory tests of pressure loss with a flowing solids-gas system are not considered reliable because of entrance and exit effects coupled with low pressure losses within the system which magnify potential errors.

Laboratory Tests

The primary objectives of laboratory tests were to determine: (1) the effective particle density as opposed to total particle mass on volume requirements when drilling with gas, (2) the effect of solids concentration on particle lift, (3) the point at which solids choking occurs and (4) the point at which solids are the most difficult to lift in a vertical flow column.

Description of Equipment

A schematic diagram of the laboratory equipment used in these investigations is shown in Plate I. Air was supplied by a 25-horsepower rotary blower run by a 25-horsepower electric motor shown in Plate II. The blower was capable of supplying air at two atmospheres of pressure,

PLATE I

SCHEMATIC DIAGRAM OF THE LABORATORY EQUIPMENT

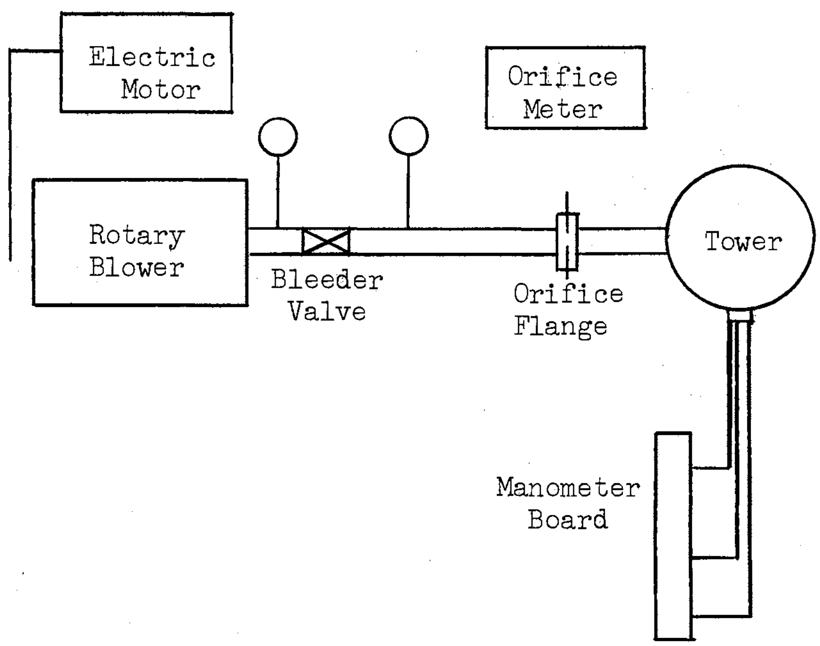


PLATE II

ROTARY AIR COMPRESSOR

which was more than adequate for the purposes of this work. Also the use of the rotary type air device made it possible to obtain a steady flow of air with no surge effects. This eliminated the necessity of using an air volume tank. Air volumes were controlled by (1) changing belt sheaves for large changes in required volumes and (2) by a variable opening needle or bleeder valve in the air line.

The measurement of air volumes was accomplished by the use of a calibrated orifice meter in the manner shown in Plate III. Pressure and temperature gauges were used upstream from the meter run and readings were taken every few seconds during experimental tests.

The flow chamber for this series of tests was a 4-inch internal diameter lucite tube, which was 20 feet long. This flow tube is shown in Plate IV. Particles could be observed visually through the transparent pipe. Located adjacent to the lucite tube is a 20 foot section of 4-inch steel pipe. This pipe was not used in this series of tests. Pressure taps in the lucite tube were located at 3.67, 12.67, and 19.0 feet from the bottom of the pipe. In all cases the particles were suspended in the flowing air stream above the lowest pressure tap. Pressure differences in the flow chamber were measured by manometers shown just to the right of the steel pipe in Plate IV. Kerosene was used in the manometers to increase the accuracy of pressure measurements.

Particles were put into the flow system at the base of the lucite tube. This was accomplished by a lubricator system such that the rate of particles injected could be controlled. Particles and air were discharged to the atmosphere at the top of the flow chamber. No attempt was made to maintain a controlled back pressure.

PLATE III

LABORATORY ORIFICE METER INSTALLATION

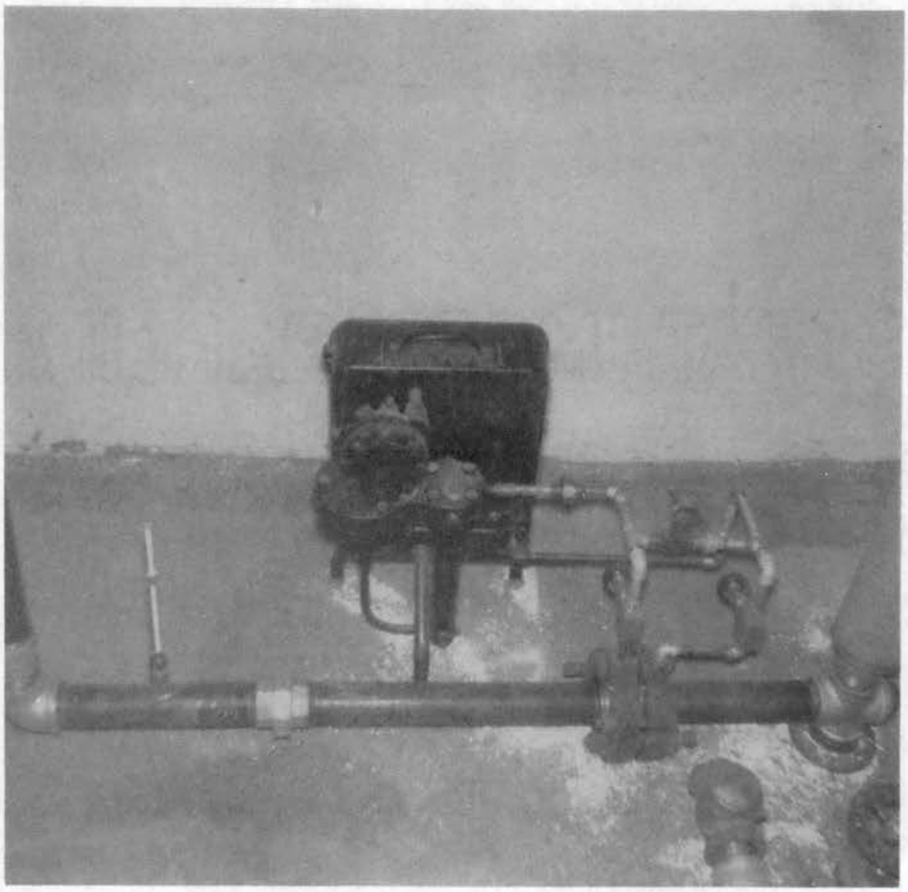
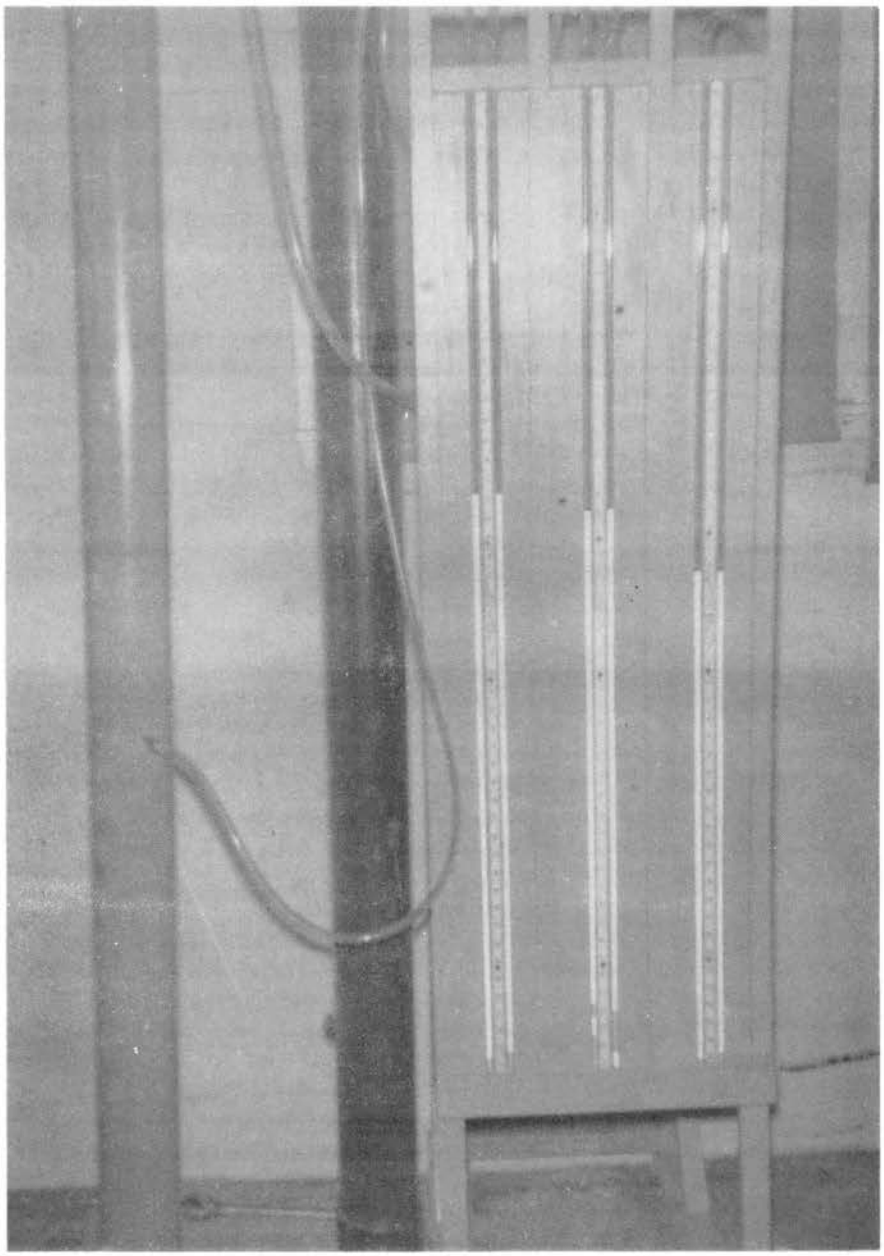


PLATE IV

LABORATORY FLOW CHAMBER



Subrounded plastic and glass particles were used in these tests. The glass particles had an average diameter of 3.4 millimeters and weighed 150 pounds mass per cubic foot. The plastic particles were the same size but weighed 73 pounds mass per cubic foot.

Pressure and temperature measurements were made every few seconds while circulating air. The primary purpose of these measurements was to determine the volumes of air being used and to determine the loss of pressure due to the presence of particles. This particle pressure loss was determined by recording the pressure loss in the system at given rates of air flow with no solids, then measuring the pressure loss at the same air flow rates using a known mass of solids. The difference in pressure loss was taken to be the particle pressure loss.

Laboratory Results

During the laboratory experiments, the particles were suspended in the flow chamber such that the particle slip velocity equaled the air velocity. As would be expected the particles showed a tendency to move towards the walls of the flow chamber. There is no particular significance that can be attached to this phenomena, since the normal velocity distribution would tend to cause particles to leave the middle of the flow stream, where the velocity of air is a maximum. Also the particles on most occasions moved up the pipe in a spiral motion. Because these particles entered the flow chamber from a perpendicular flow line it is possible that this was a result of a centrifugal action of the air in the flow chamber.

During one series of tests an effort was made to determine the effect of temperature on the lifting capacity of air. Air temperature was varied 22°F and no noticeable change was observed in the mass of air required to suspend particles. This is an expected result since theory predicts that the mass of gas required to lift particles is inversely proportional to the square root of absolute temperature. This is shown as follows:

$$G = \frac{Z}{\sqrt{T}}$$

For example, the change in temperature was from 600°R to 622°R. A simple calculation shows that the required mass of gas at 622°R should be 1.015 times the mass of gas required at 600°R. This is such a small change it is not surprising that it was not detected in experimental results.

The purpose in considering temperature was to determine whether it was necessary to include temperature as a variable or some average value when calculating gas requirements. These laboratory tests showed the effect of varying temperature was negligible. The field tests showed the temperature at 3,750 feet, which was just above the bit for this series of tests to be 120°F and the temperature at the flow line discharge to be 60°F. It can be shown that an average temperature between the surface and 3,750 feet can be used with no measurable sacrifice in accuracy relative to the pressure loss in the annulus.

The first series of laboratory experiments were made to determine the effect of particle density on particle pressure drop and gas requirements. In these tests, pressure loss was determined for the glass and plastic particles only. This was done by measuring the pressure loss in the flow chamber shown in Plate V between pressure taps 1 and 2.

The measured particle pressure loss due to both the glass and plastic particles is shown in Tables III and IV. A graph of particle

PLATE V

DIAGRAM OF TOWER SHOWING POSITION OF PRESSURE TAPS AND PARTICLE FLOTATION

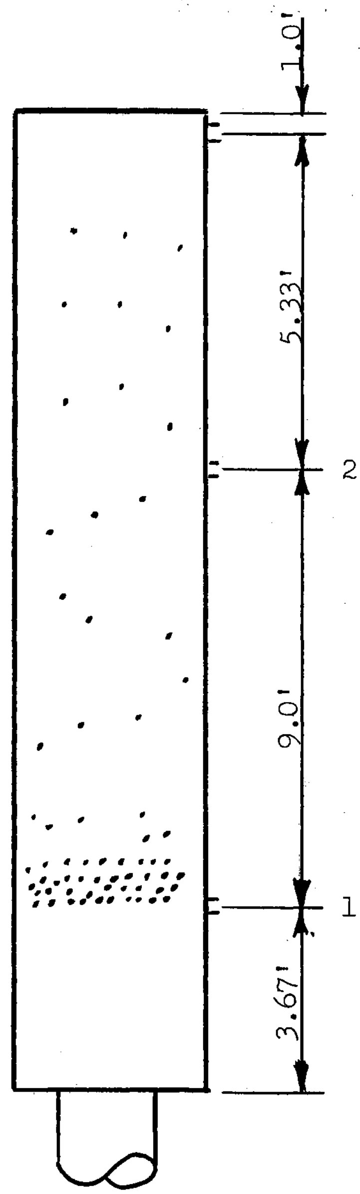


TABLE III

3.4 MM GLASS PARTICLES

Run No.	Mass Grams	Q SCF/Hr	Particle Pressure Drop Inches, H_2O
67	10	11,500	.024
68	20	11,100	.049
69	30	11,200	.071
70	40	11,500	.094
71	50	11,400	.116
72	60	11,400	.131

TABLE IV

3.4 MM PLASTIC PARTICLES

Run No.	Mass Grams	Q SCF/Hr	Particle Pressure Drop Inches, H_2O
12	5	7,460	.01
13	10	7,450	.02
14	15	7,410	.03
15	20	7,410	.04
16	25	7,410	.05
17	30	7,410	.06
18	35	7,400	.07
19	40	7,400	.08
20	45	7,380	.09
21	50	7,350	.11
22	55	7,330	.12
23	60	7,330	.13

pressure loss versus mass of both glass and plastic particles is shown in Fig. 13. A comparison of these data show that particle pressure loss is a function of total mass and essentially independent of individual particle density. From this it is concluded that: (1) individual particle density is not an independent variable relative to particle pressure loss and (2) the pressure loss due to particles was due primarily to air-particle loss not particle-particle or particle-wall losses. These conclusions are reached because it is obvious that the plastic particles would outnumber glass particles by a ratio of 2:1 for the same mass. These results are important in the determination of pressure loss in the annulus of gas drilled wells.

Drag coefficients calculated from data in Tables III and IV ranged from 0.856 to 0.995. These are higher than those predicted by past theory for spheres (0.44), but are in the same range as Gray's results using sandstone (0.805) and shale (1.40) particles.

These data also show that the effects of solids concentration on gas requirements is reflected completely in the pressure change effects. This means that with pressure staying constant, the amount of gas required to lift particles of a given size would be independent of the total number of particles. This is a conclusion reached by previous investigators and this work offers confirmation of this fact. Referring to Tables III and IV, it can be seen that the air required to suspend cuttings remained the same as the concentration of particles was increased. This was particularly true for the glass particles. For the plastic particles the amount of air required for suspension decreased slightly after the mass of particles exceeded 40 grams. The cause of this slight reduction in required air volume is believed due to the fact that the

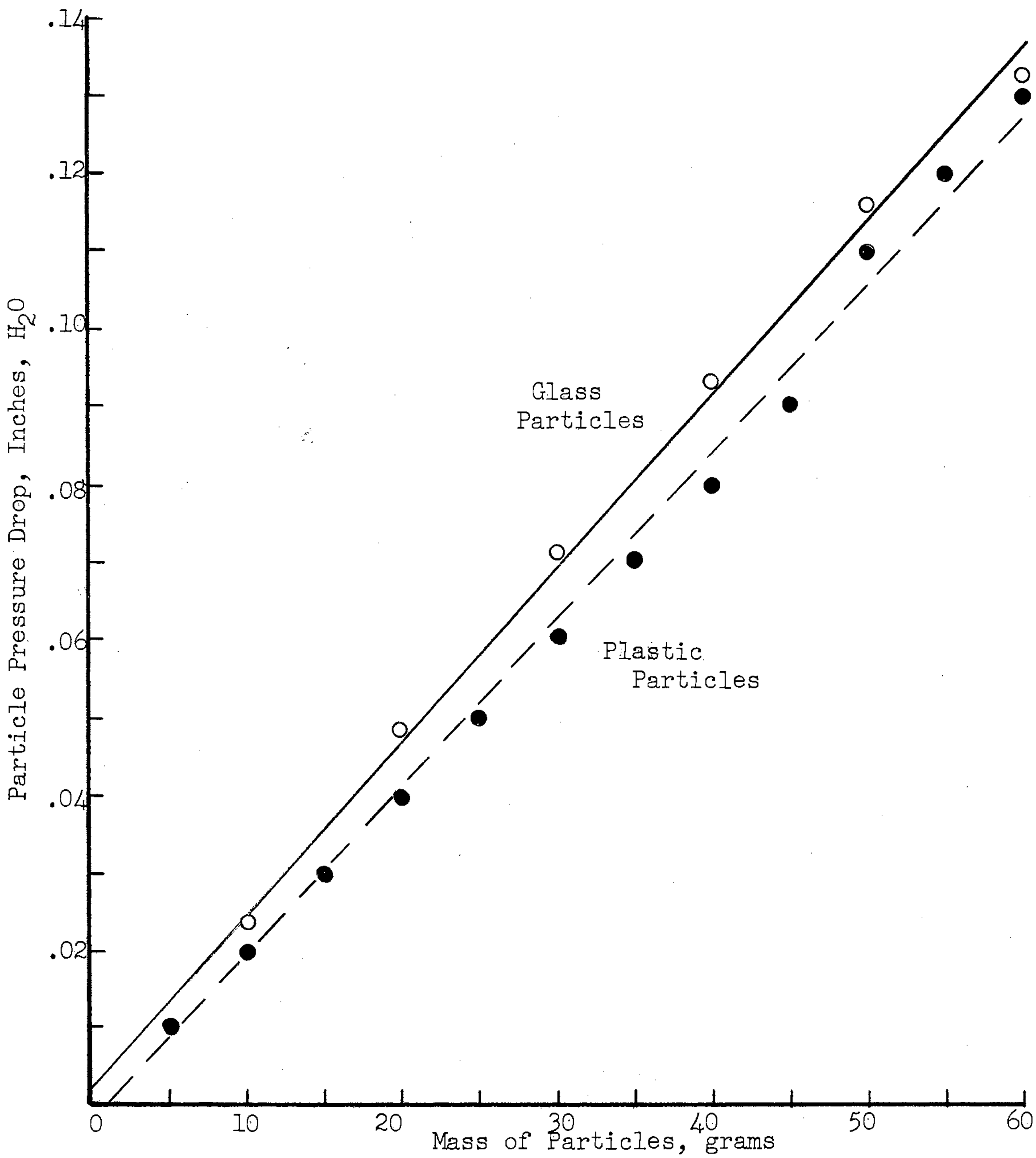


Fig. 13. Particle Pressure Drop vs. Mass of Particles for 3.4 mm Particles

resulting air velocity was increased due to the decrease in void space. This suggests that some optimum range of solids volume to gas volume might be obtained in normal gas drilling operations. However, this optimum would probably have to be obtained in a region where small increases in solids volume might choke-off particle lift entirely.

No further efforts were expended in the check of drag coefficients for the following reasons: (1) there is an abundance of information available on drag coefficients and in almost all of the work the drag coefficients vary from 0.44 to 1.4 depending on test conditions, (2) the work completed by Gray appears to be reliable for the purposes of this study since his work included studies at concentrations of more than one particle and the particles selected were actual field samples and (3) it would be almost impossible to predetermine the shape of formation cuttings, which must vary in shape and size in the annulus.

The next phase of this experimental work was directed towards a determination of the effect of extremely high concentrations of solids. This study was made because of the zone of solids concentration which may occur at the top of the drill collars. In the summary of previous work, Zenz showed that at a given concentration of solids, in the so-called choking range, the lift of solids may reduce to a slugging action. As the concentration of solids increases further, a fluidized bed is formed and the removal of solids may be choked-off completely.

This choking action could be a serious problem in gas drilling and might be minimized if the problem could be recognized in normal drilling operations. To test this phenomena a series of tests were conducted by adding 3.4 mm glass beads to a constant rate flow stream until solids choking occurred. The results of these tests are shown in Table V.

TABLE V

SOLIDS CHOKING RUNS

Run No.	Mass Grams	Temperature °F	Static Pressure Psig	hw Inches H_2O	Condition of Flow
73	50	123	0	98	Lift
74	100	138	0	98	Lift
75	150	144	0	100	Breakdown
76	150	138	0	98	Breakdown
77	131.5	146	0	98.5	Breakdown
78	121.0	159	0	99	Breakdown
79	109.0	160	0	99	Breakdown

It is noted that a choking-off of particle lift consistently occurred when the mass of glass particles exceeded 100 grams. Calculations show that choking in these tests occurred when the solids content by volume of gas was about 4 percent. Other investigators have shown that solids choking may occur when the ratio of solids to gas volume is no more than two percent. In these tests, the cause of solids choking can be explained as follows: As the concentration of particles is increased, the effective velocity acting on the particle is increased. As long as this small increase in effective velocity offsets the additional friction loss, the volume of air required for suspension remains fairly constant or may even decrease slightly as shown in Table IV. However, at some point the stacking of particles, one above the other, results in an increase in mass without a corresponding increase in drag area. As this stacking of particles continues the small increases in air velocity due to the reduction in void space are not enough to offset the increase in effective mass per unit of drag area. Thus at some point the continuous lift of particles is choked-off.

This description of a solids choking can be correlated with actual gas drilling operations in the following manner: consider the potential point of solids build-up at the top of the drill collars. Solids which are too large to lift further may be suspended in the zone at the top of the drill collars. As drilling progresses these solids are broken into smaller pieces by the rotation of the drill pipe and may be lifted out of this zone. In the meantime large particles are still entering the zone. Thus an equilibrium concentration of solids occurs. A sudden increase in the drilling rate, a slight decrease in the air volume or an increase in the back pressure on the annulus might result suddenly in solids choking and these solids would fall back around the top of the drill collars. This could result in stuck drill pipe and the end of gas drilling not only in that particular well but in that particular area of operations, because any indication of hole trouble may result in no more gas drilling operations. This increase in the accumulation of solids might be detected at the surface by a small increase in pressure loss or perhaps in additional rotating torque. Thus the warning may be small and the result expensive unless precautions are taken when the problem first occurs. There are many possible solutions. The most simple solution is to increase the air volume. If this is not possible it may be necessary to decrease the rate of penetration. Another possibility is the raising of the drill pipe while circulating. This would tend to move the zone of solids accumulation to a higher level in the annulus and might be enough to initiate a removal of the solids.

This problem of solids accumulation prompted another series of tests which were concerned with the effect of insufficient air volumes on lift and the possible consequences if adequate volumes of air were not available. In this particular experimental system air entered the 4-inch flow chamber from a 2-inch line. Thus a venturi effect was created which is similar to that created as air moves from the drill collar-hole to the drill pipe-hole annulus.

By varying the air flow rate at volumes below that required for lift, the height reached by the particles was altered. From this and knowledge of the point at which the particles suddenly moved up the flow chamber, a correlation was obtained between the percent of the air volume required for lift versus the fraction of the height required. This result is shown in Fig. 14 and has added significance if analyzed carefully. Fig. 14 shows that if the air volumes required for lift are 90 percent of those required for the actual cutting removal, the solids will reach a height of only 60 percent of that required for removal up the annulus. A small deficiency in gas volumes may cause a considerable amount of difficulty in gas drilling. For example the accumulation of solids above the collars would be in a much shorter interval and therefore the rate of solids accumulation would be much more critical. As shown in Fig. 14 this problem is increased substantially as the percent of required air volume is reduced below 90 percent.

Another problem investigated in this work was the point at which lift is most difficult. This was attempted in the laboratory circulating system. However, conclusive results were not possible because of the extremely small changes in pressure. A numerical analysis for a 9,000 foot well using 1,000 foot intervals was investigated using an IBM 1410 computer. These results are shown in Tables VI and VII.

It is noted from Tables VI and VII that the mass of gas required for lifting a given size particle increases with well depth. Actually this mass of gas is primarily a function of pressure if an average annulus temperature is assumed. From this numerical evaluation it can be seen that the most difficult point of particle lift in gas drilling is at the point of highest pressure. Thus any back pressure imposed by surface connections may have a substantial effect on volume requirements for gas drilling. Also it is noted that the gas requirements are increased slightly as the ratio of the mass of particles to the mass of gas is increased. This does not contradict previous statements, since the increase in gas requirements is due to the increase in pressure

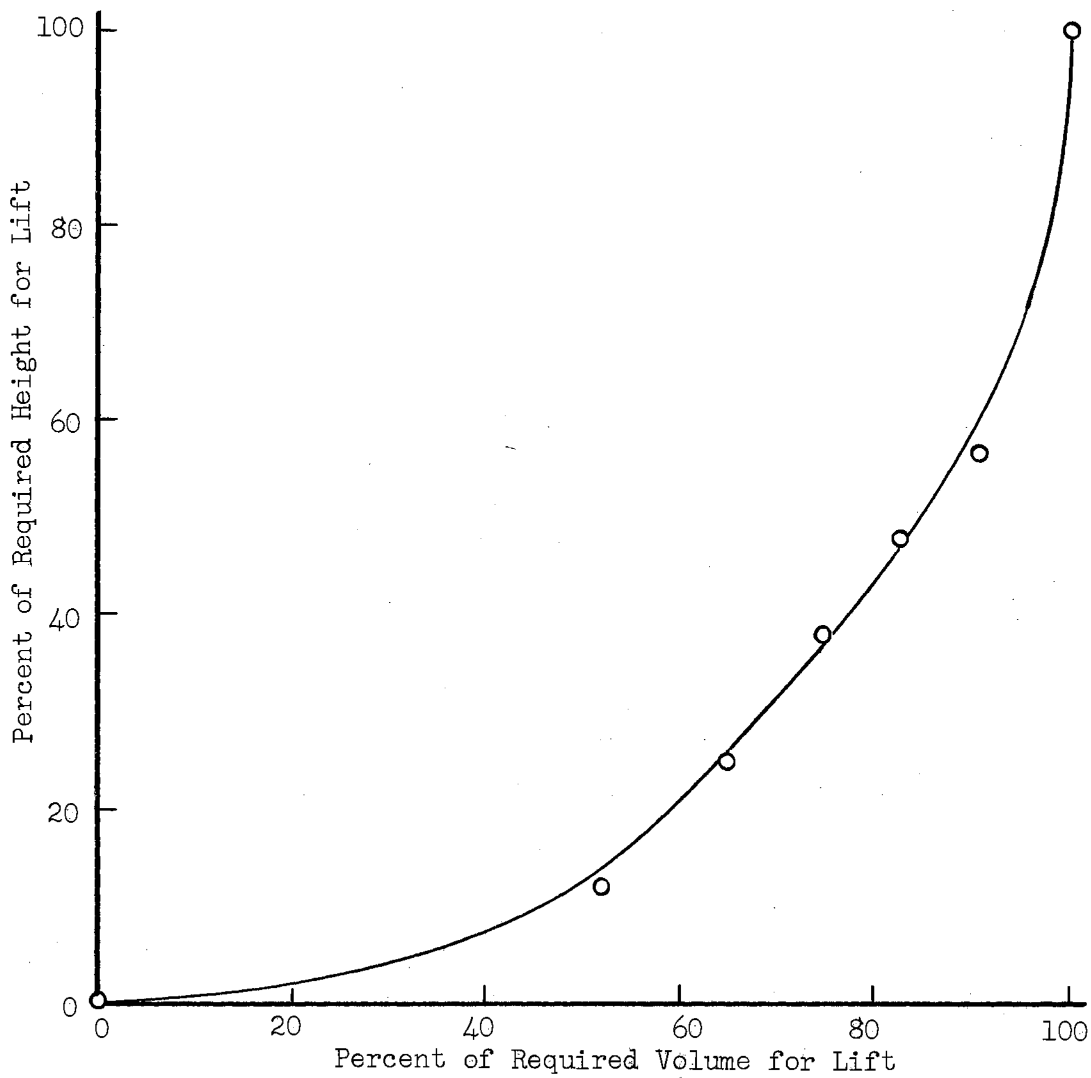


Fig. 14. Percent of Required Height vs. Percent of Required Volume

TABLE VI

AIR FLOW RATES VERSUS WELL DEPTH

(Particle Size Equals 0.1 Inch)

	Hole Size 7-7/8 Inches		Drill Pipe Size 4-1/2 Inches	
Depth ft.	Pressure (psia)	Flow Rate (SCF/min)	Pressure (psia)	Flow Rate (SCF/min)
0	14.70	410.4	14.70	410.4
1,000	15.97	423.3	16.10	424.4
2,000	17.25	437.0	17.49	439.4
3,000	18.56	452.4	18.93	454.3
4,000	19.92	467.8	20.42	471.1
5,000	21.32	485.0	21.98	488.0
6,000	22.78	503.9	23.59	506.8
7,000	24.28	522.9	25.27	527.5
8,000	25.84	543.8	27.01	548.4
9,000	27.45	566.6	28.82	571.4
	$M_p/M_f = 0.1$		$M_p/M_f = 0.2$	

TABLE VII

AIR FLOW RATES VERSUS WELL DEPTH

(Particle Size Equals 0.1 Inch)

	Hole Size 9 Inches		Drill Pipe Size 4-1/2 Inches	
Depth (ft)	Pressure (psia)	Flow Rate (SCF/min)	Pressure (psia)	Flow Rate (SCF/min)
0	14.70	596.8	14.70	596.8
1,000	15.64	616.2	15.73	6.517
2,000	16.64	636.7	16.86	635.8
3,000	17.73	636.7	18.03	658.3
4,000	18.84	659.4	19.24	681.0
5,000	19.98	682.2	20.50	706.1
6,000	21.16	707.3	21.81	706.1
7,000	22.38	734.8	23.18	733.7
8,000	23.64	762.6	24.59	761.7
9,000	24.96	762.6	26.06	792.4
	$M_p/M_f = 0.1$		$M_p/M_f = 0.2$	

imposed by the increase in solids. Of specific interest in Tables VI and VII is the magnitude of the increase in gas requirements from the surface to total depth. For example in Table VII using $M_p/M_f = 0.1$, the calculated pressure is 14.7 psia at the surface and 24.96 psia at the total depth of 9,000 feet. For an increase of 10.26 psia the gas requirements for lifting particles has increased 27.8 percent. This is a significant increase and illustrates the importance of watching very closely the surface pressure gauge when drilling with gas.

In summary Tables VI and VII indicate that a particle that could not be removed at 9,000 feet might be lifted at 8,000 feet if in some way it could attain that level. This increase in gas requirements with depth is certainly not a new concept but the magnitude of increase as a function of small increases in pressure has not been emphasized in past work.

Field Tests

Field tests were made in two wells being drilled with air in Southwestern Arkansas. These tests were conducted to determine (1) the pressure loss in the annulus while actual drilling operations were in progress and (2) to obtain if possible from observations the minimum air volumes required to move formation cuttings up the annulus.

Field Equipment

A double pole mast drilling rig capable of drilling to 10,000 feet was used on this job. Plate VI shows a schematic diagram of the internal part of the circulating system. The piping would be considered standard except for the mandrel just above the bit which shows a side opening. This side pocket or opening in the mandrel was used to put a

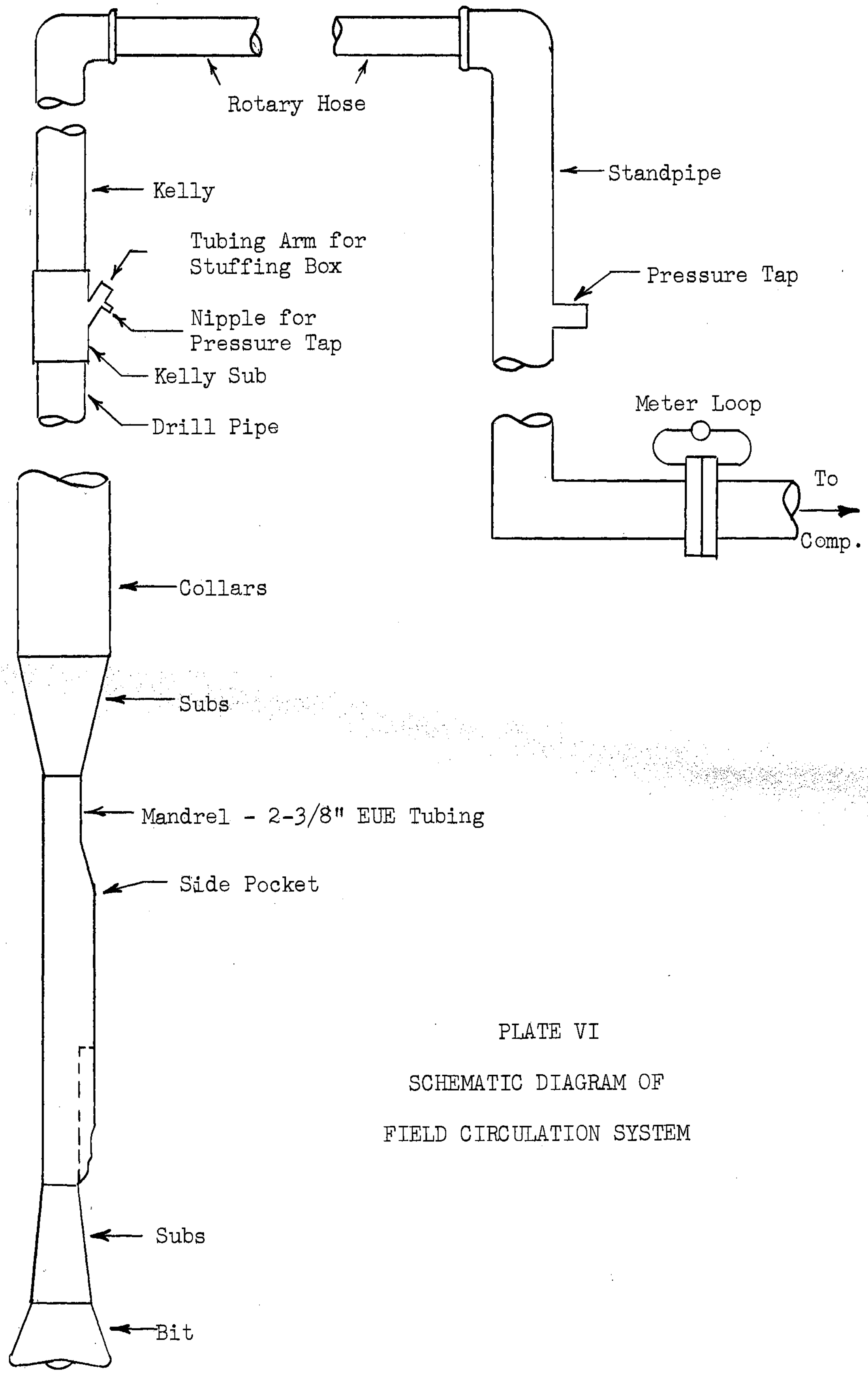


PLATE VI

SCHEMATIC DIAGRAM OF

FIELD CIRCULATION SYSTEM

pressure recording device into the annulus. A cross-section of this mandrel is shown in Plate VII. Plate VIII shows two views of the side pocket opening on the mandrel assembly suspended in the derrick of the drilling rig. Wireline assemblies were run through the lubricating device shown in Plate IX. Pressure measurements were made with pressure gauges that were frequently calibrated with a dead weight tester. Air volumes were measured with a standard orifice meter. Plate X shows the orifice meter and gauges used at this location for measurements of pressure. Plate XI shows pressure on the output air from the compressors; this gauge is also shown in Plate X. These field tests were run during a period beginning in December 1964 and ending in March 1965.

Field Test Procedure

Pressure data using down-hole measuring devices could be obtained only with the bit suspended off bottom and the drill string stationary. For this reason all of the actual down-hole data were obtained at 3,800 feet. From these data, calibration curves were constructed for pressure losses through the individual parts of the drill string. These calibration curves were then used with recorded surface pressures to determine pressure losses within the drill string while drilling at other depths.

Pressure tests at 3,800 feet were made inside the drill string, just above the bit, and in the annulus just above the bit. Flow rates were varied from 590 to 1,238 SCFM and all pressure measurements were made and checked at five different flow rates. Pressure measurements were also made at the compressor discharge and at the stand-pipe on the drilling rig floor. This procedure permitted the determination of

PLATE VII

CROSS-SECTION OF MANDREL WITH SIDE OPENING

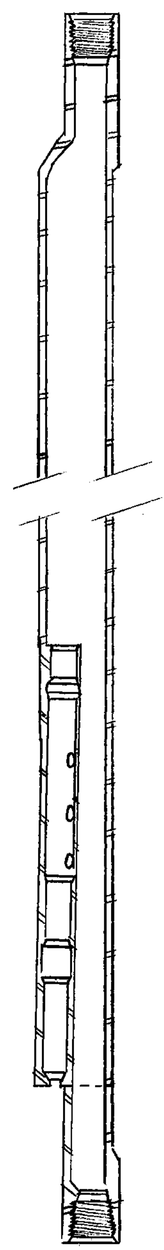


PLATE VIII

MANDREL SUSPENDED IN DERRICK

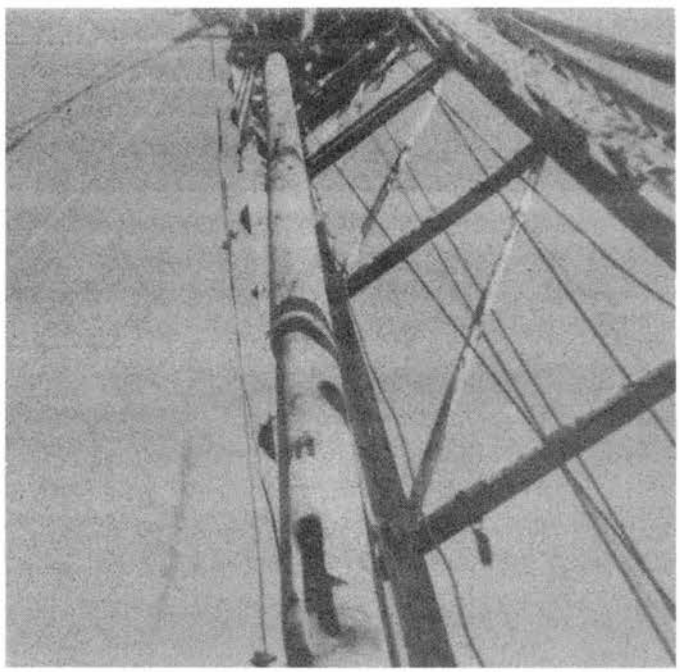


PLATE IX

LUBRICATING DEVICE

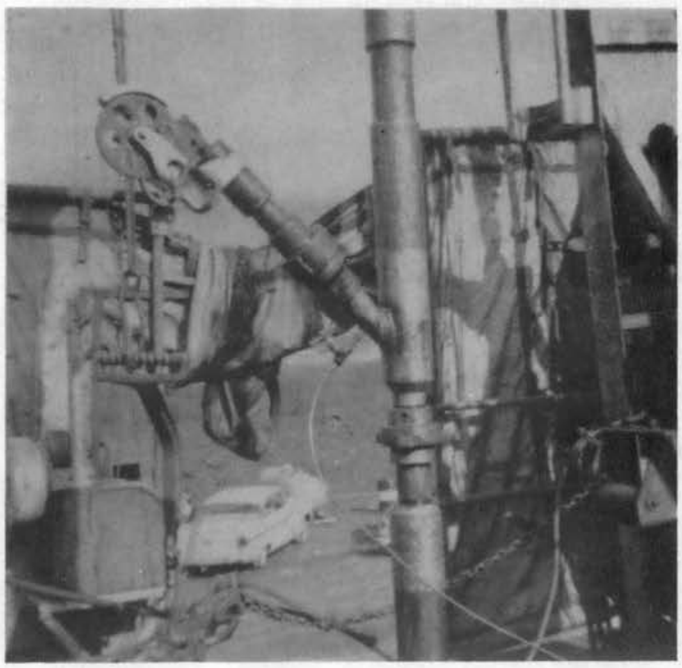


PLATE X

ORIFICE METER INSTALLATION

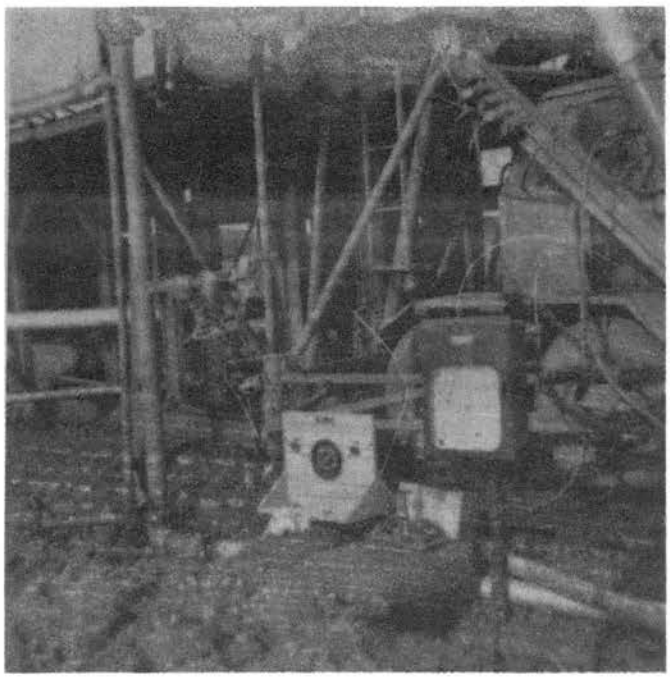
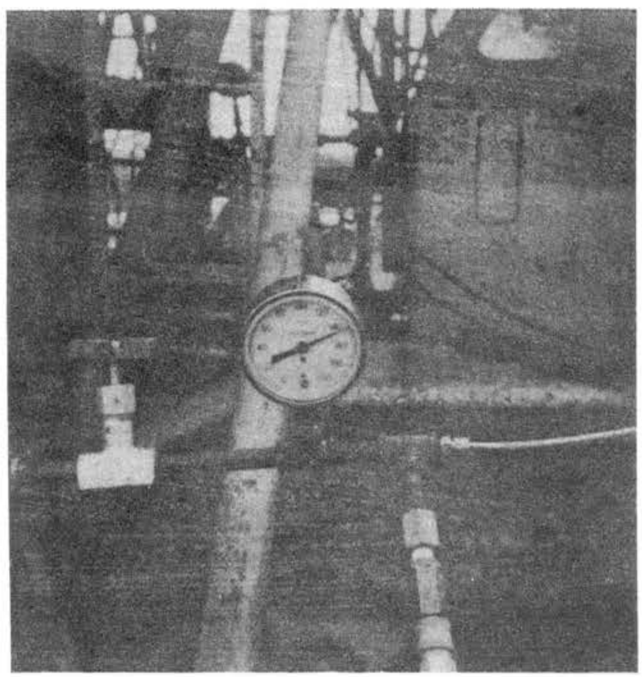


PLATE XI

AIR PRESSURE GAUGES



pressure loss through the bit, inside the drill pipe and in the annulus while circulating air with the bit just off bottom. When drilling operations were commenced pressure measurements were made at the same surface locations and increases in pressure were assumed to be due to the solid added to the flow stream. This permitted the determination of pressure losses in the annulus while drilling at given rates. Although actual down-hole pressure measurements were made at only the 3,800 foot level, the data were extended for deeper depths by the use of the calibration curves and comparisons between field data and theoretical calculations were also made at 4,488, 5,038 and 5,919 feet. These tests were made at drilling rates of 28, 29, 42 and 61 ft/hr. In addition to the pressure data, drilling rates were observed as a function of air circulation rates and these results are compared with design rates and pressure levels in the annulus. The methods used to construct calibration curves for pressure losses in the drill string and for measuring annulus pressure losses are given in Appendix C.

Field Test Results

A summary of results is shown in Table VIII. Included in this table is a comparison between field test results and those obtained using the theoretical equation developed by the writer and those obtained using the equation proposed by Angel. Graphical comparisons of these results are shown in Fig. 15, 16, 17 and 18.

Fig. 15 shows comparisons between annulus pressure losses determined from field measurements, those calculated using the theoretical equation developed by the writer and those calculated using Angel's pressure loss equation. These tests were run at 3,812 feet while

TABLE VIII

SUMMARY OF RESULTS

Well Depth feet	Flow Rate SCFM	Annulus Pressure Loss, psia From Field Data	From Theory	From Angel's Equation
3,812	659	30.44	32.97	21.36
	733	31.82	33.13	22.00
	869	34.41	33.17	23.78
	1,028	37.32	34.76	24.96
	1,215	40.71	36.37	27.11
4,488	571	29.40	33.95	21.22
	678	30.37	33.84	22.01
	752	30.85	33.96	22.63
	825	31.24	34.21	23.30
	1,001	31.80	35.18	25.06
	1,179	31.93	36.39	27.03
5,038	584	37.83	36.85	22.55
	698	38.48	36.35	23.35
	761	38.21	36.30	23.88
	1,008	37.47	37.01	26.28
	1,189	35.28	38.20	28.30
5,619	593	43.50	40.33	24.23
	709	44.57	39.28	24.90
	805	44.67	38.87	25.62
	877	44.56	38.72	26.22
	1,043	43.37	39.06	27.87

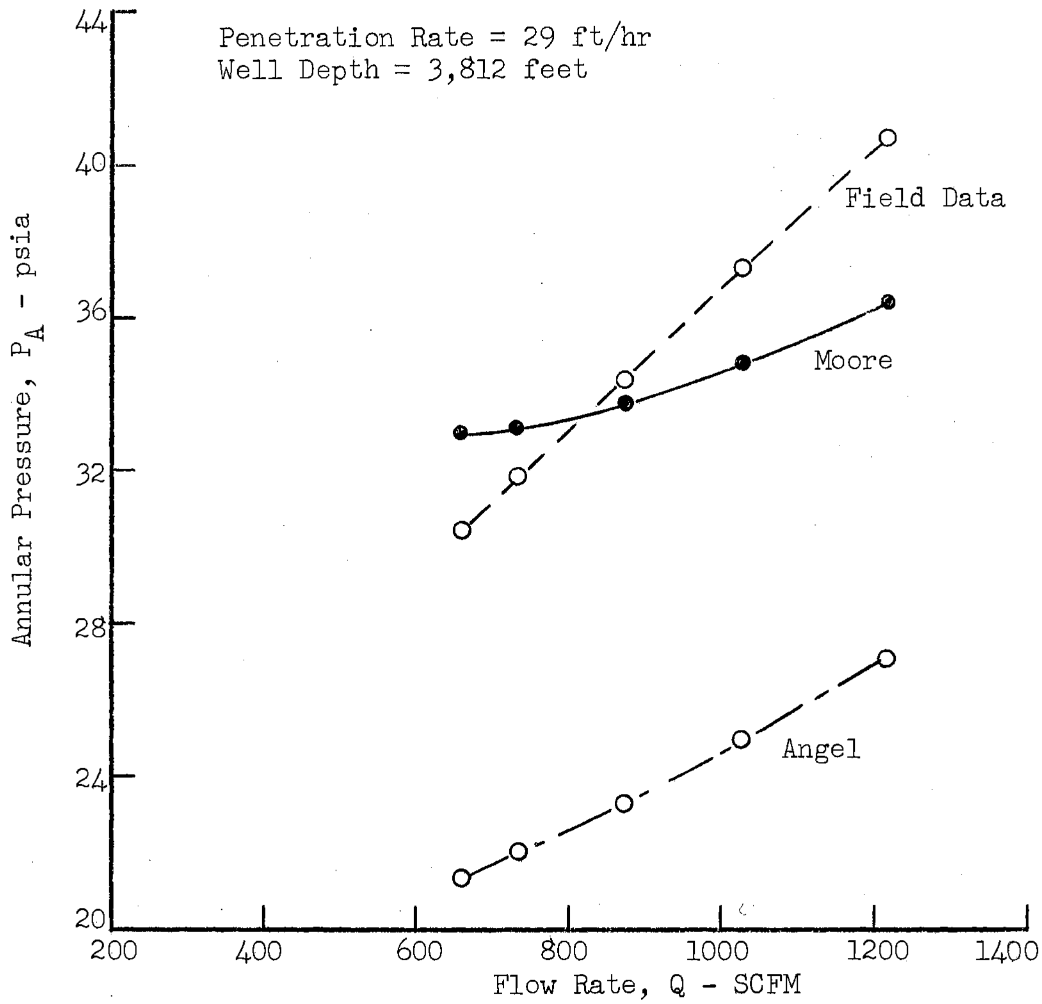


Fig. 15. Annular Pressure vs. Flow Rate

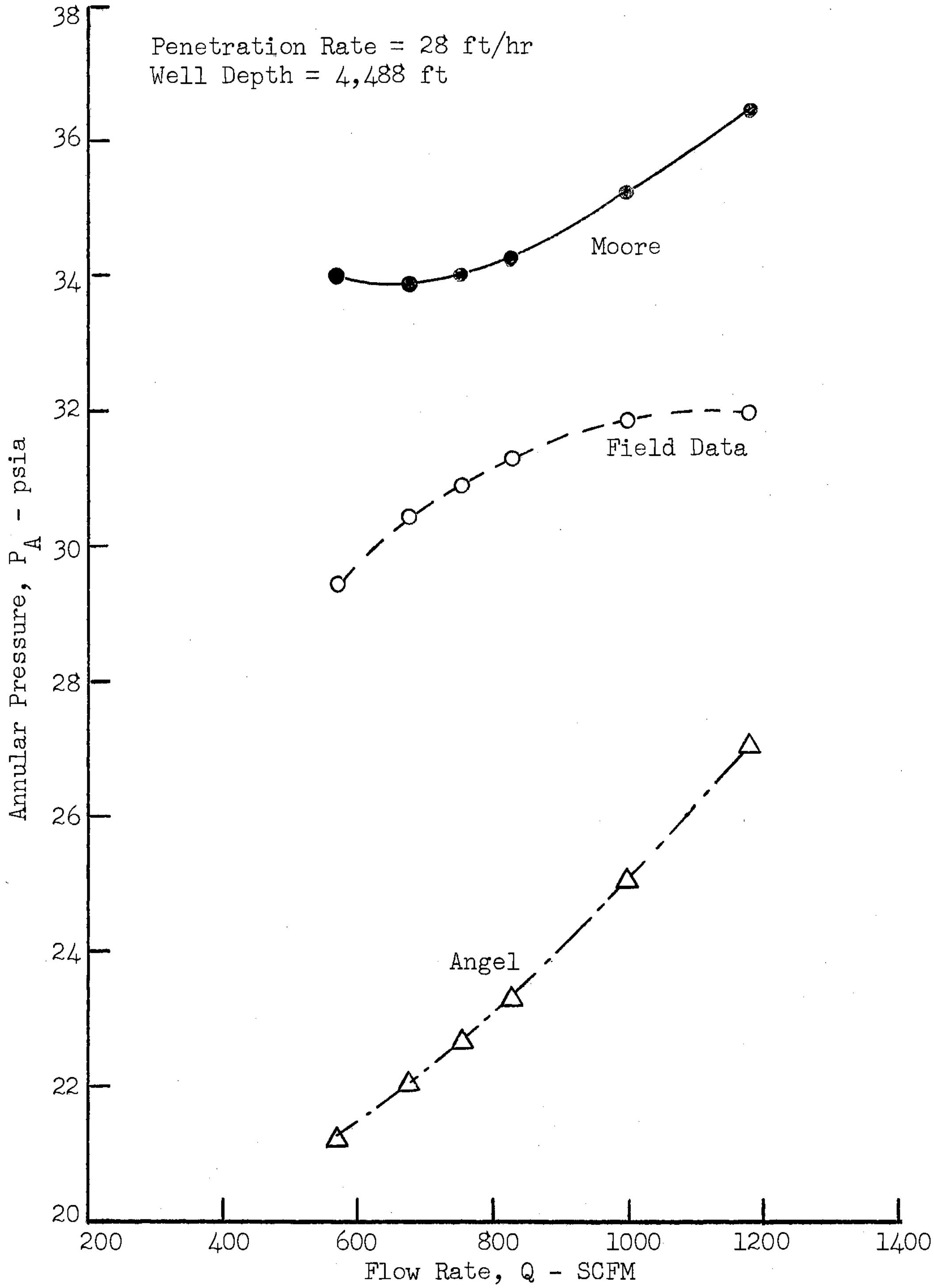


Fig. 16. Annular Pressure vs. Flow Rate

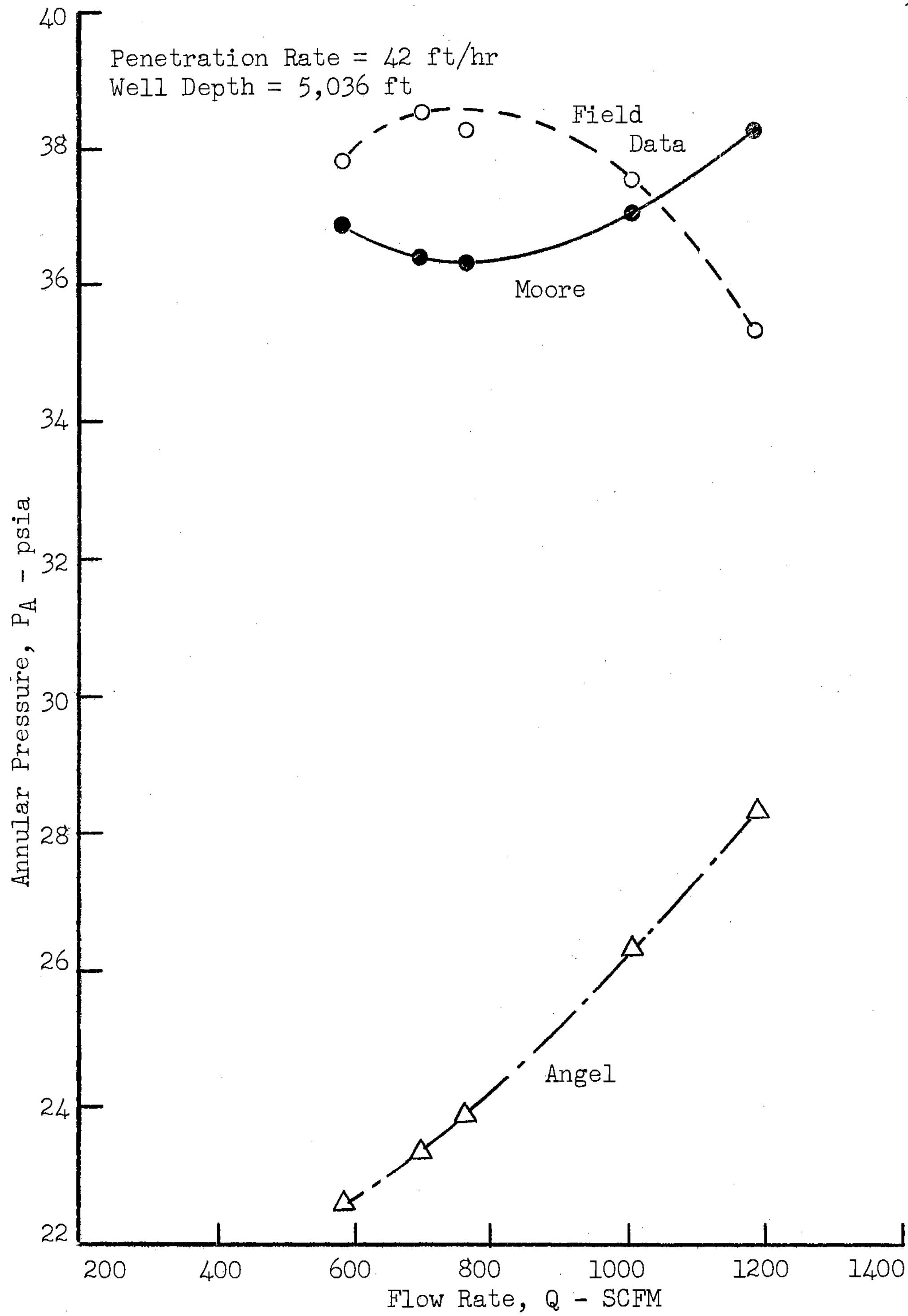


Fig. 17. Annular Pressure vs. Flow Rate

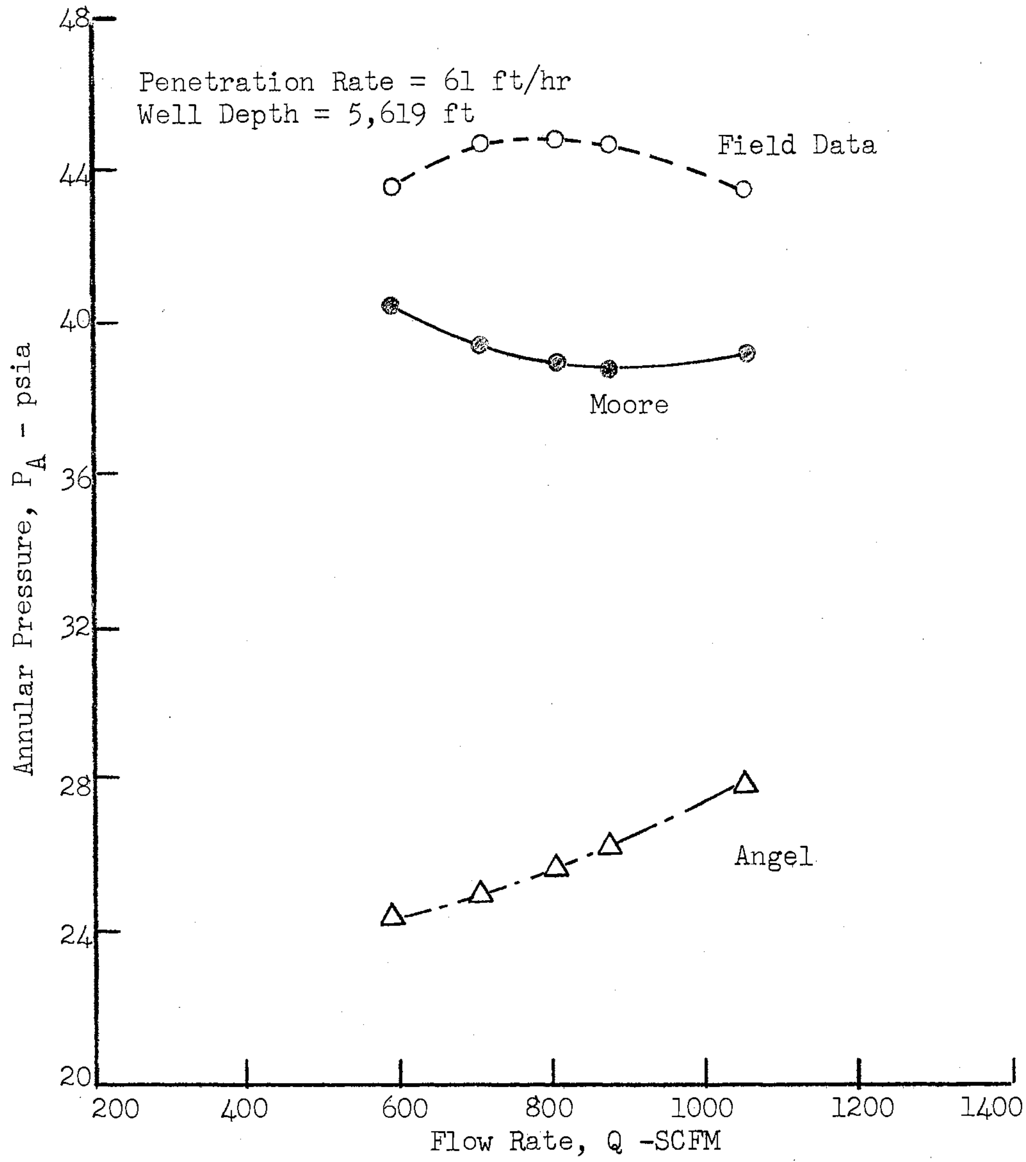


Fig. 18. Annular Pressure vs. Flow Rate

drilling at a rate of 29 ft/hr. The pressure loss through the pipe and bit at this depth had been measured previously, thus the annulus pressure losses were determined by subtracting these losses from the recorded surface pressure at the various rates of air flow. The maximum variation in pressure loss was observed at a flow rate of 1,215 SCFM, where field data indicated the pressure loss to be 40.7 psig and theoretical calculations show this pressure loss to be 36.4 psia. This is a difference of 4.3 psia or a variation of about 10 percent.

In subsequent tests annulus pressure losses were determined by using the calibration curves shown and discussed in Appendix C. The results of these tests are shown in Fig. 16, 17, and 18 which include comparisons at depths of 4,488, 5,038, and 5,919 feet while drilling at rates of 28, 42 and 61 ft/hr. The curves for the field results do not follow the expected trends. Under normal conditions, these curves should follow the same pattern as the calculated curves. The test procedure followed may be responsible for the shape of these curves. Measurements were initiated at the low rates of air flow and increased in five steps to the maximum rate of air flow. Pressures at the surface were recorded at each rate of flow after a 5 minute waiting period. It is possible that this waiting period was not long enough to clean the annulus of solids. Future field tests run for this purpose should be conducted by starting at the maximum air velocity and reducing by increments. In any event the order of magnitude for pressure loss in the annulus seems to compare favorably for the theoretical and field results. At a drilling rate of 28 ft/hr shown on Fig. 16, the maximum difference in pressure loss was 4.5 psia. At a drilling rate of 42 ft/hr as shown on Fig. 17 the maximum pressure difference was less than 3.0 psia. The largest variation in values occurred at a drilling rate of 61 ft/hr while drilling at 5,919 feet; these results are shown on Fig. 18. Pressure differences between field and theoretical results were as high as 6.0 psia. The maximum variation showed theoretical calculations to be about 13.7% below field results at an air flow rate of 850 SCFM.

Because particle-particle and particle-wall collisions have been neglected in the theoretical equations it is possible that at the higher drilling rates that the omission of this phenomena may result in calculated values of pressure that are conservative.

It was not possible in these drilling tests to determine with accuracy the optimum amount of air either to remove cuttings or to maximize drilling rates. However in one series of tests shown on Fig. 19, there appeared to be an optimum air velocity relative to maximizing penetration rate. It was noted that in each drilling test that penetration rates could be increased by increasing air volumes up to some given point, where the rates of penetration in most circumstances were reduced by further increases in air flow rates. A question as to why the increased air flow rates might actually reduce penetration rates is introduced. For some insight into one probable cause, compare the air flow rates for the minimum theoretical pressure calculations with those for maximum penetration rates. In the test at 3,812 feet the calculated minimum pressure in the annulus occurred at a flow rate of 650 SCFM and the maximum rate of penetration occurred at a flow rate of about 750 SCF. In the test at 4,488 feet the minimum pressure loss

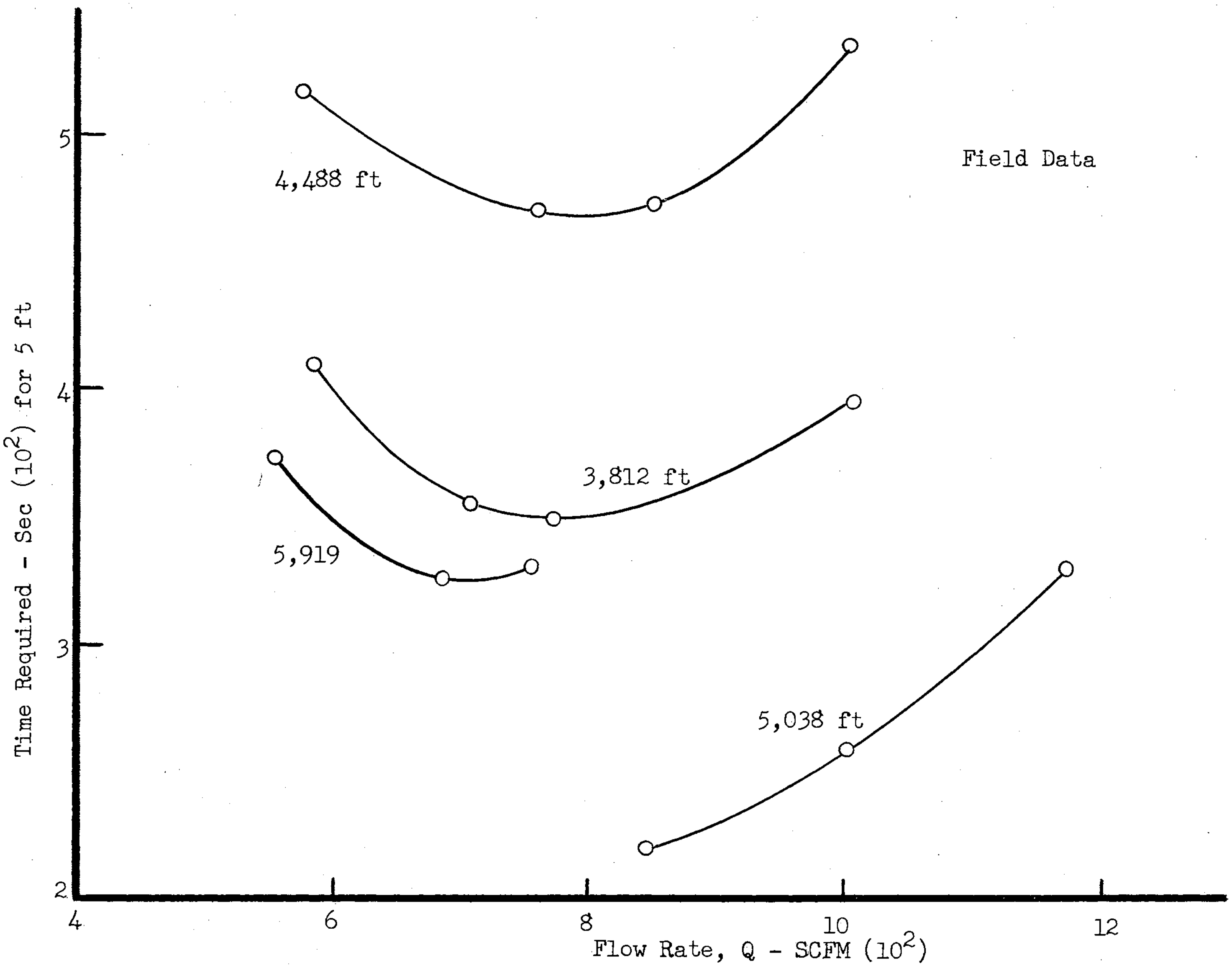


Fig. 19. Drilling Rate vs. Air Flow Rate

was shown to be at a flow rate of 675 SCFM and the maximum penetration rate at just under 800 SCFM. In the test at 5,038 feet the minimum calculated annulus pressure loss occurred at just above 750 SCFM and in this same test the maximum penetration appears to occur at just under 800 SCFM. Data on penetration rates for the 5,919 feet depth were not considered reliable.

These data were checked in more than one drilling test and there appears to be a definite relationship between air volumes and penetration rate. It is noteworthy that the maximum rates of penetration were so closely related to the minimum calculated pressure losses. While no definite conclusions can be drawn from these tests, they do show that excess flow rates of air can be detrimental and emphasize the need for properly designed gas drilling programs. It is also indicated in these tests that the optimum quantities of gas may very well correlate with minimum calculated annulus pressures. This is a significant probability and further field tests are needed to confirm this correlation. If it is confirmed, optimum volume requirements for gas drilling could be based on the rate of flow that resulted in a minimum calculated pressure loss. This flow rate would be the optimum required for lifting cuttings, for maximizing drilling rate and for minimum power requirements at the surface.

These experimental tests have shown that a program designed on the basis of a maximum solids content of 4 percent by volume of gas would probably have no basis for use. Attempts to drill with very low air volumes were in general unsuccessful. This occurs because when volumes are slightly less than those required to lift the cuttings, solids loading occurs rapidly. This means that in most gas drilling operations the operator must maintain a volume ratio of solids to gas of one percent or less.

The correlation of the calculated minimum pressure point as a function of penetration rate and volume rates of air flow with the air flows required for maximum drilling rate is a significant result. This same point of minimum pressure correlates with the minimum calculated gas volumes required in the design programs presented in the theory of this work. As a result the operator can no longer afford the luxury of having too much gas, because not only is he spending additional money for gas or compressor capacity he is also reducing the penetration rate. While the field results in this case are preliminary and certainly should be checked further they are startling enough to cause the operator to examine current programs where excess gas may be used.

One question that may remain in the mind of readers is the fact that the field results show pressure at a maximum where calculated values were at a minimum. This is a point of concern and raises a question concerning the validity of the field pressure measurements. It appears from these tests that the annulus was loading with solids and that as air volumes were increased the volume ratio of solids to air was being reduced. Also it may be possible that calculated pressure losses inside the pipe and through the bit were not completely accurate, and thus in subtracting these losses from surface measurements, the determination of annulus pressure loss was slightly in error.

From the results of the drilling rate tests and experience relative to the fact that annulus pressure reductions should increase drilling rate, it is concluded that the calculated annulus pressure losses are as reliable as those reported in field results. It is believed that these field results show the theory in this to be reliable enough for general use; however it is suggested that further testing would be desirable.

CHAPTER V

COMPARISONS WITH PREVIOUS WORK

In the discussion of experimental work, annulus pressure losses for gas-solid mixtures were determined in field tests. These results were then compared with calculated annulus pressure losses using two theoretical equations. One of these pressure loss equations is new and has been presented for the first time in this research, the other equation was introduced by Angel and has been accepted and used, by the oil industry for more than eight years. This chapter will show comparisons, using the same two equations, with laboratory data obtained by Williams. In addition comparisons will be made between gas volume requirements predicted in this research work and those predicted in work presented by Angel.

The primary purpose in comparing the theory with previous laboratory data is to show the expected pressure trends versus gas velocity using gas-solids mixtures. Although quantitative results from laboratory tests are of questionable value, the qualitative trends should be reliable. The comparisons on gas volume requirements are presented to show that predicted gas volumes from this work are considerably lower than those required in results from Angel's work.

Angel's equation in differential form for the annulus pressure loss to be expected from a gas-solid mixture is rewritten for quick reference.

$$\frac{dP}{dh} = \rho_m \left[1 + \frac{v^2 f}{2g_c (D_h - D_d)} \right] \quad (9)$$

The equation for annulus pressure loss developed in this work is also repeated for convenience.

$$\frac{dP}{dh} = \rho_f \left(1 + 5 \frac{M_p}{M_f}\right) + \frac{\rho_f v^2 f}{2g_c (D_h - D_d)} \quad (52)$$

Calculated results using Equations (9) and (52) are compared with laboratory data obtained by Williams. Williams used sand feed rates of 14 and 35 lb_m/ft^2-sec and a flow chamber with a 1.05 inch internal diameter. Since he was using a constant feed rate for sand, the ratio of solids mass to fluid mass is a function of the input air velocity. The methods used to determine the pressure loss using Equations (9) and (52) are shown in Appendix D.

Fig. 20 shows Williams laboratory results versus calculated pressure losses for a flowing mixture of sand and air, where the sand feed rate is kept constant [14 lb_m/ft^2-sec] and the air flow rate is varied. The agreement between the theory and laboratory measurements is poor at low air velocities. A much better agreement is observed at high air velocities. However, the use of the concept of mixture density, ρ_m, and Equation (9) represented by the top curve in Fig. 20 shows these values are much higher than laboratory results. Another comparison using a solids feed rate of 35 lb_m/ft^2-sec is shown in Fig. 21. Again at low flow rates the theoretical results are much higher; however, the agreement improves as the flow rate is increased.

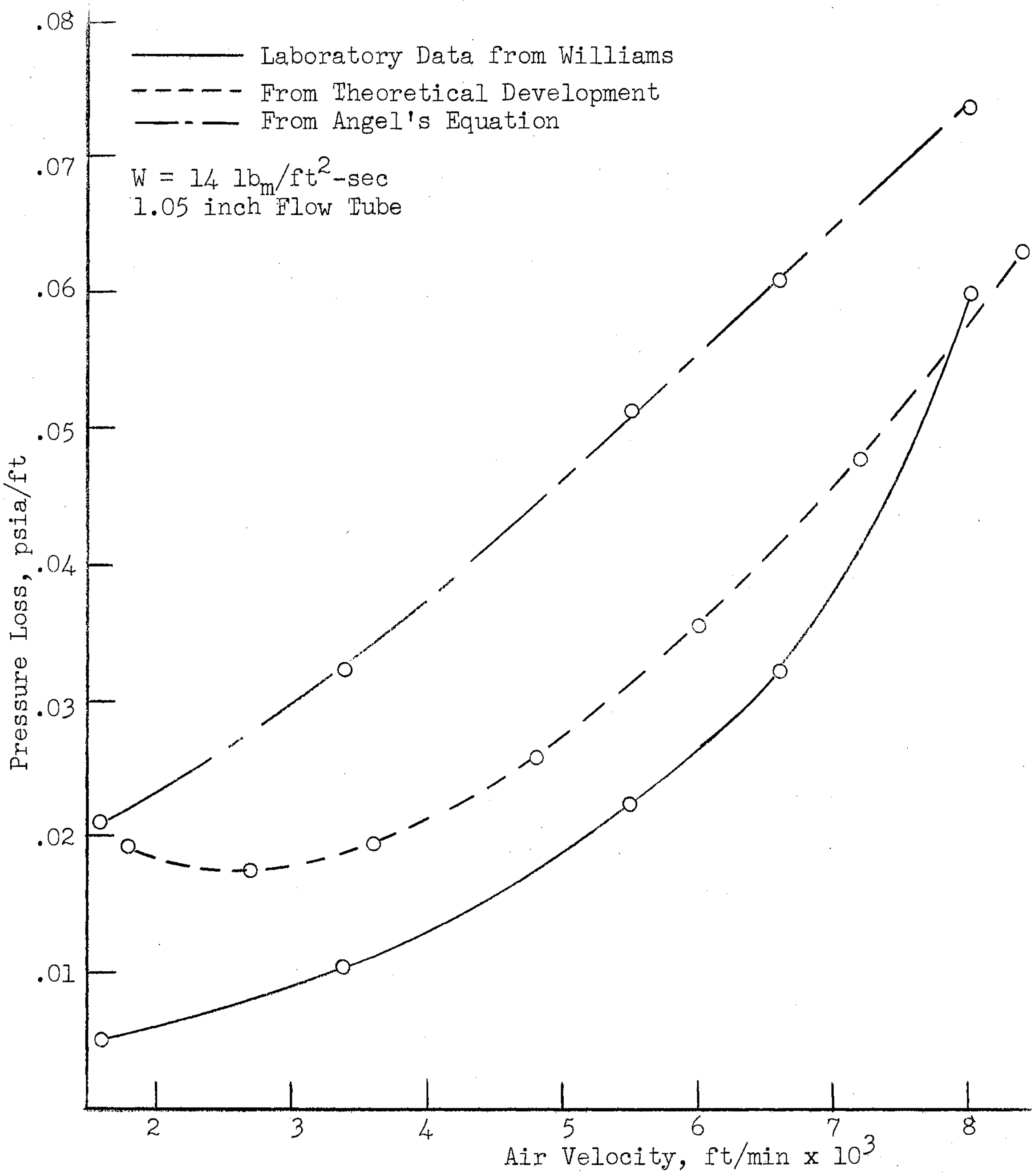


Fig. 20. Pressure Loss vs. Air Velocity Sand-Air System

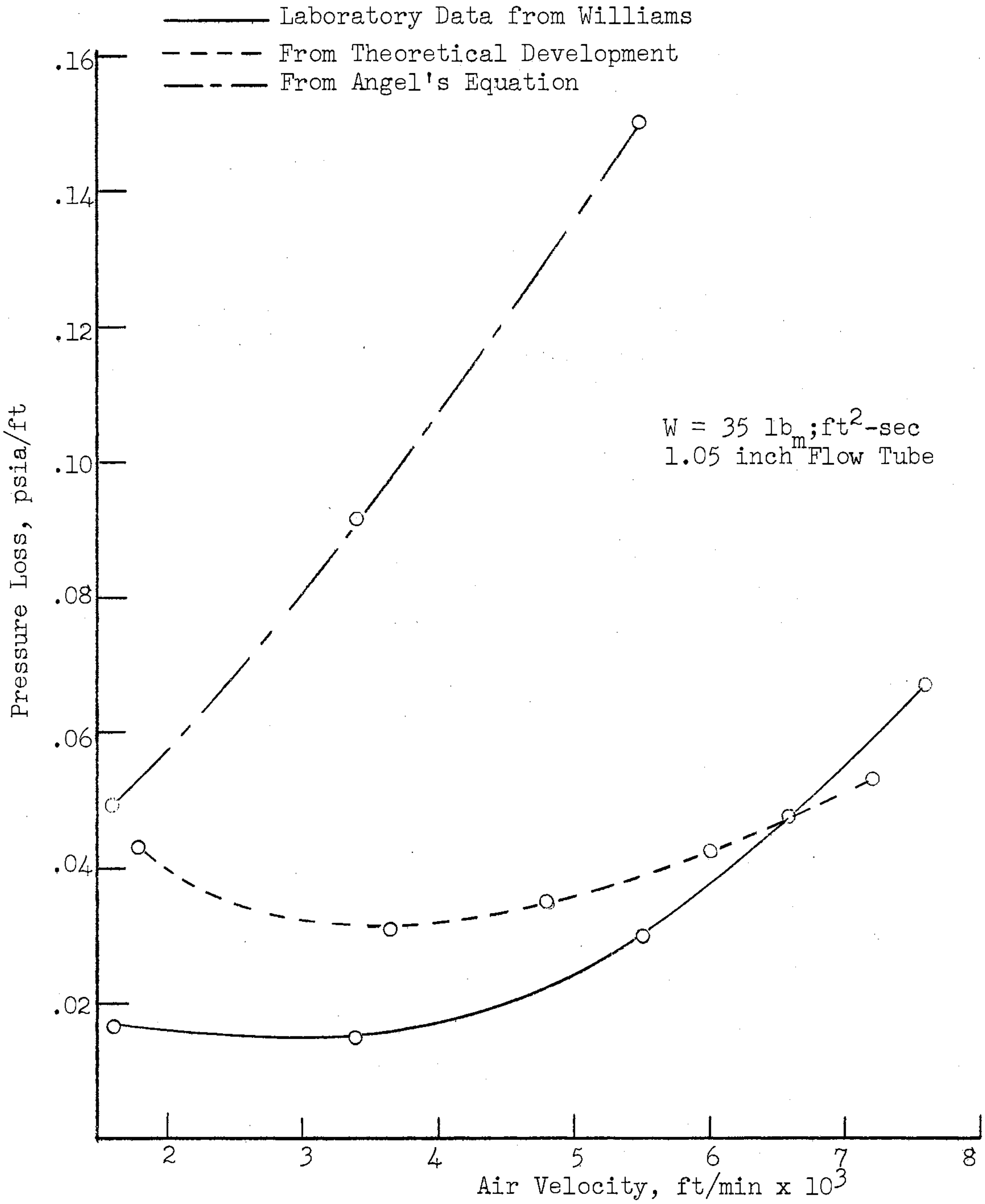


Fig. 21. Pressure Loss vs. Air Velocity

Calculated results using Equation (9) or the assumption the solids increase density by going into solution are substantially higher. At low rates of air flow, theory predicts the solids will accumulate in the flow chamber and that most of these solids will not be removed continuously until the air flow rate reaches about 3,600 ft/min. Actual results show some accumulation of solids, but show most of the solids being removed at a flow rate of 2,600 ft/min. As flow rates are increased the pressure loss developed by theory and that obtained by laboratory measurement is converging. In fact above air flow rates of 6,600 ft/min the pressure loss obtained in the laboratory by Williams exceeds the predicted pressure losses. The reasons for this behavior are believed to be: (1) at low rates of air flow, solids are injected into the flow chamber and some distance is required before they deaccelerate to a terminal velocity, thus the lower pressure loss from laboratory results occurs because solids slippage is less than predicted; (2) at high flow rates the degree of turbulence in the air and solids stream has increased substantially and the collision of solids is adding to the total pressure loss.

Of importance is the fact that laboratory results also show the accumulation solids at low rates of air flow. This is predicted by theory in this research. In the equation where solids are assumed to go into solution, no allowance is made for solids accumulation. In Fig. 21, it is apparent that the use of Equation (9) to calculate pressure losses for an air-solids stream offers no similarity with measured results.

Comparisons of gas volume requirements using the theory from this research and Angel's predicted requirements is shown in Figs. 22,

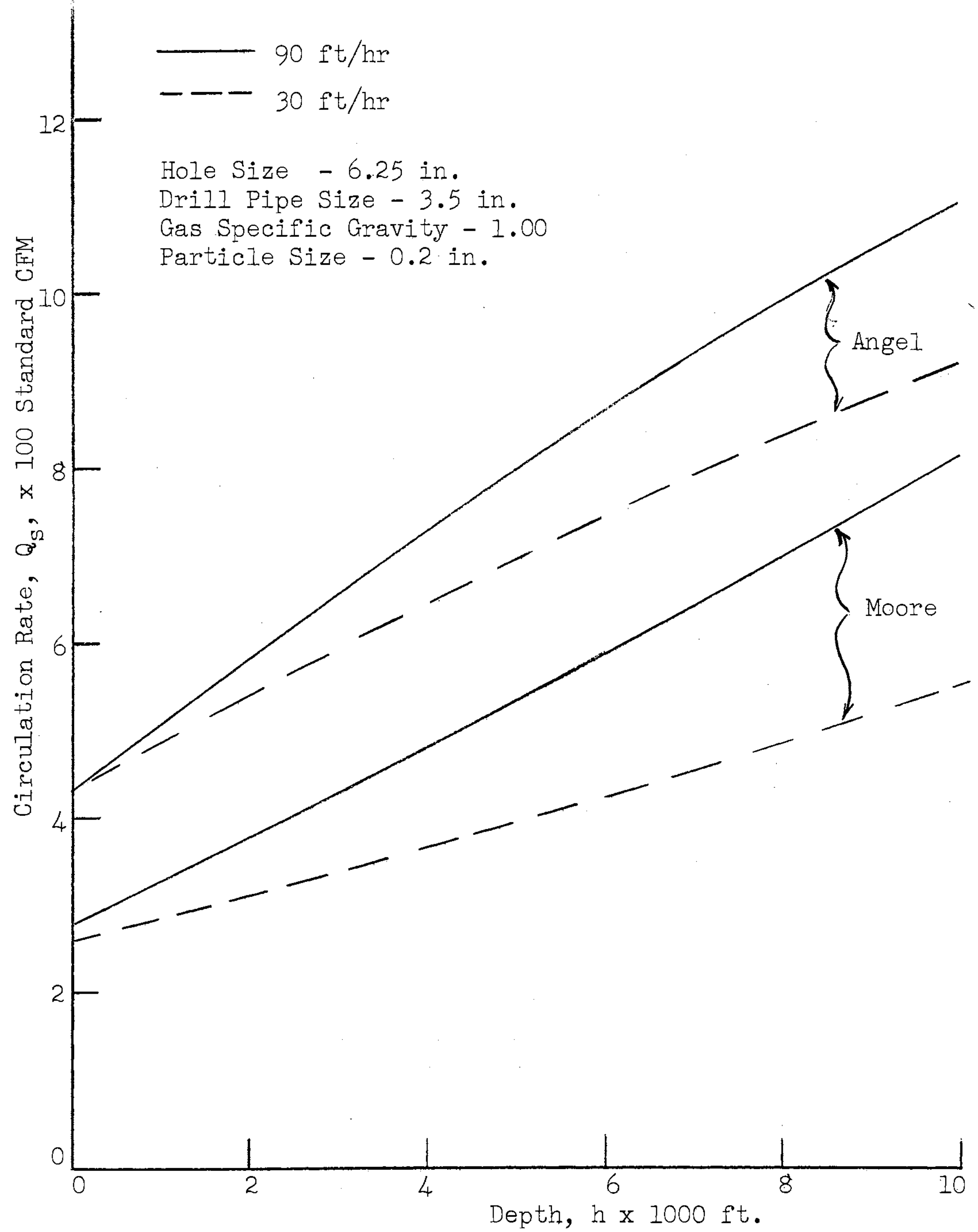


Fig. 22. Comparison of Gas Volume Requirements

23, 24 and 25. In Fig. 22 the gas volume requirements using this theory are about 40 percent less than Angel's requirements at a drilling rate of 30 ft/hr and about 35 percent less at a drilling rate of 90 ft/hr. Although there are some small variations, this difference is about the same for all depths for the given hole and drill pipe sizes of 6-1/4 and 3-1/2 inches. The same magnitude of differences are noted in Fig. 23 for the combination of hole and drill pipe sizes of 7-7/8 and 4-1/2 inches. As the combinations of hole and drill pipe are increased in size as shown in Fig. 24 and 25, the difference in predicted gas volumes is decreased slightly but is still of the same order of magnitude. The difference in predicted requirements between Angel's work and this work is due to, (1) the difference in the calculated annulus pressure losses and (2) the use of the slip velocity equation to determine lift requirements rather than using a standard air velocity of 3,000 ft/min.

The quantitative differences in gas volume requirements noted in Figs. 22, 23, 24, and 25 are significant when designing necessary equipment for a gas drilling operation. For example, consider Fig. 25 where at a drilling rate of 30 ft/hr at 10,000 feet, this writer's theory predicts a gas requirement of 1,510 SCFM and Angel's predictions show requirements of 2,330 SCFM, a 54 percent increase. Economically the operation may not be feasible if the higher volumes are actually required. Of equal significance is the fact that if the higher volumes are used drilling rates may be reduced substantially. Thus the costs of a poor design are compounded by increases in equipment and drilling costs.

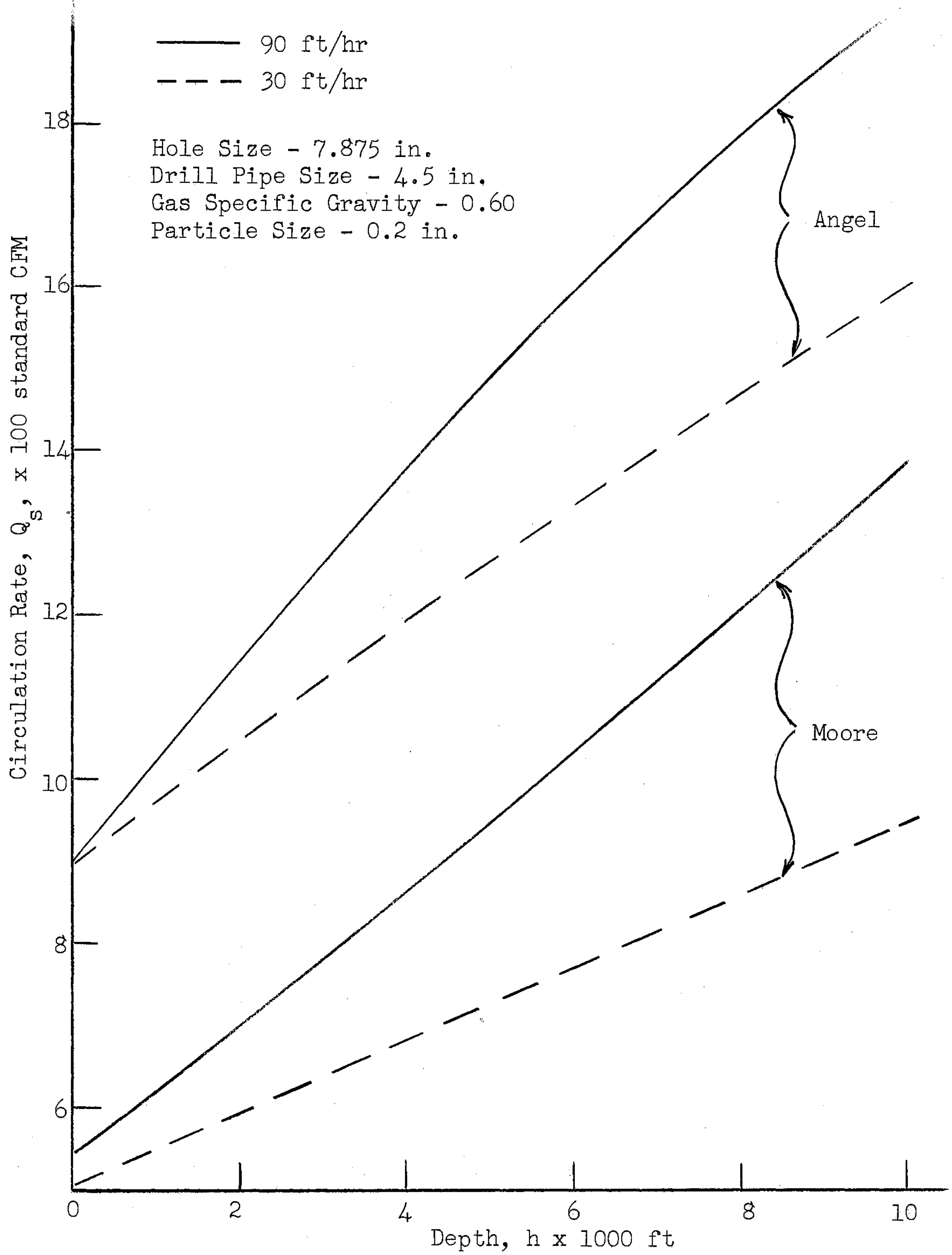


Fig. 23. Comparison of Gas Volume Requirements

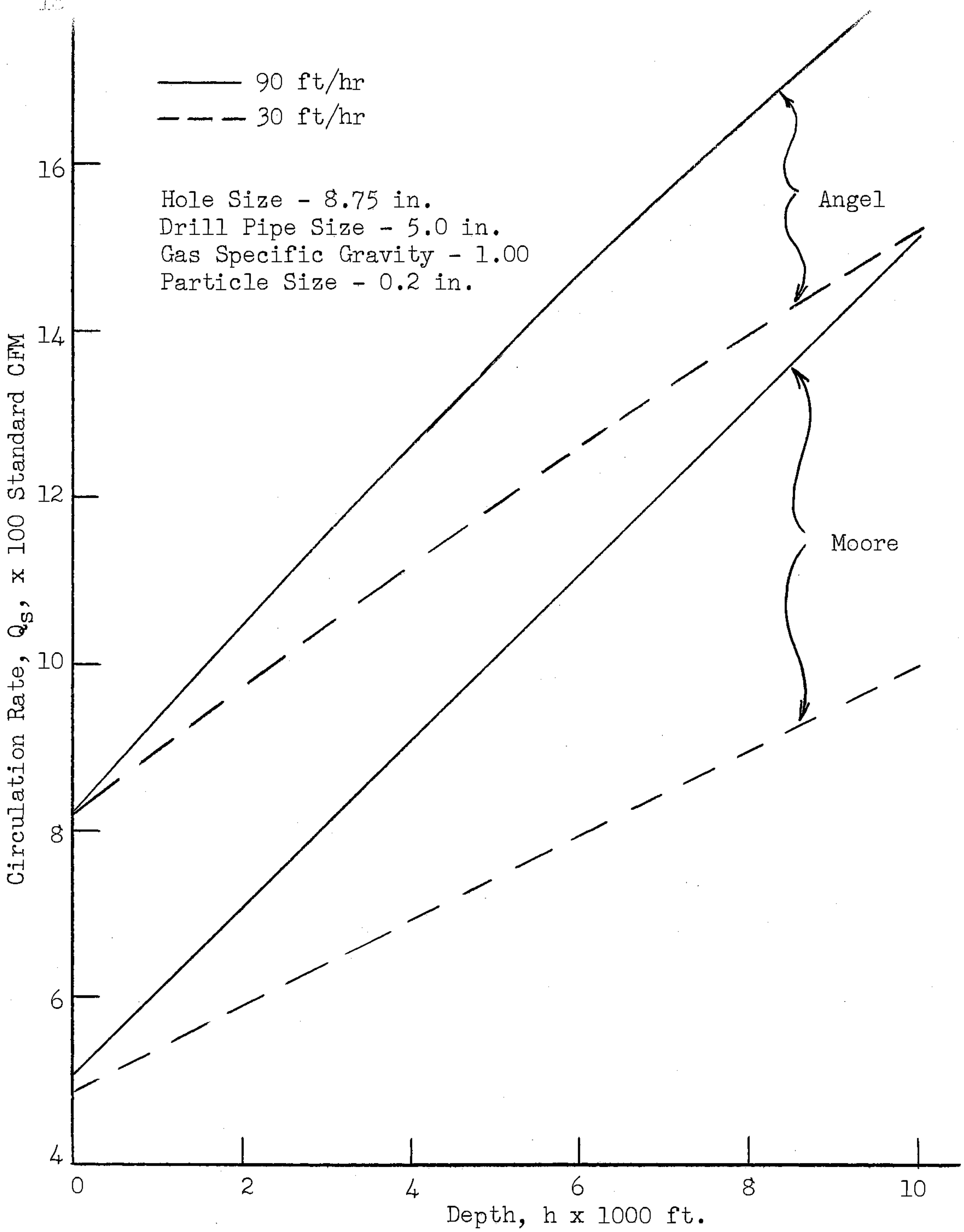


Fig. 24. Comparison of Gas Volume Requirements

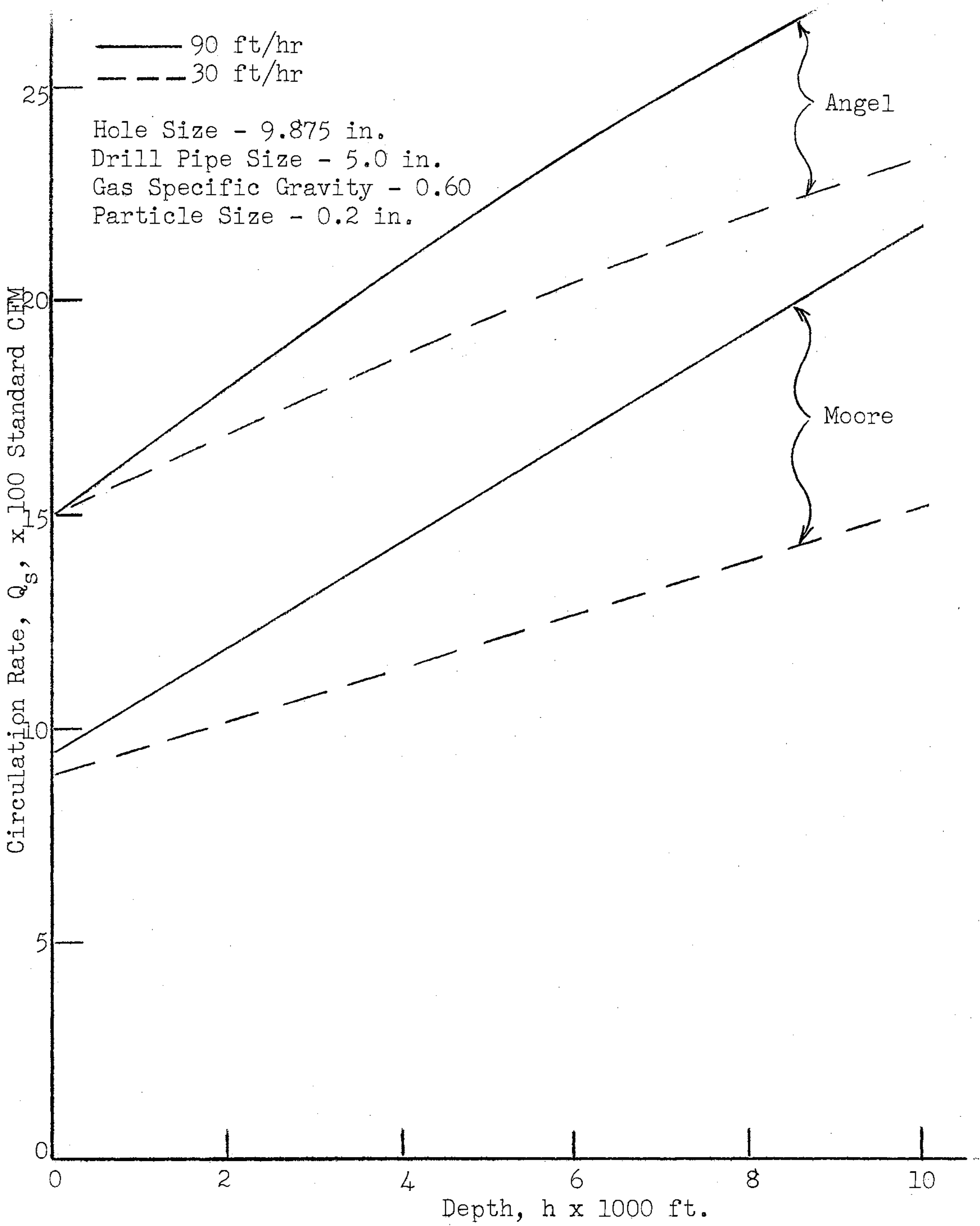


Fig. 25. Comparison of Gas Volume Requirements

CHAPTER VI

SUMMARY AND CONCLUSIONS

Gas has been used as a circulating medium in rotary drilled wells since the latter part of the decade from 1940 to 1950. Drilling rates using dry gas have been shown in some cases to be 10 times greater than those using normal drilling mud. These phenomenal results brought quick acceptance of gas drilling by the oil industry. Many oil companies plunged into the use of gas with the philosophy that volume requirements can be determined experimentally. Subsequent technical interest developed, and in 1957, Angel introduced a good initial basis for determining volume requirements. This work was followed by McCray and Cole in 1959, who presented a similar approach to Angel's; however, because they used a different friction factor their results showed a need for still higher gas volumes. This ended the efforts towards any theoretical consideration of volume requirements in gas drilling. Since 1959, any further developments have been based on trial and error methods in field practice. This research was initiated because the results from field practice showed the gas volumes predicted by past theoretical approaches was much higher than needed in actual drilling operations.

Instead of searching for methods to extend or modify past theoretical work, it was decided to start with known fundamental concepts and develop new equations for predicting gas volume requirements. The first step necessary was the development of an equation that could be used to predict the pressure loss for a flowing mixture of gas and solids. There were obvious faults in previous developments, because of the use of a density term where it was assumed that the solids went into solution. In this research the general energy balance was used to account for the loss of energy in circulating the gas and solids as separate units of mass. This is obviously a sound approach, the only limitations are the use of simplifying assumptions to reduce the difficulty of solution. The assumption that pressure loss for the solids is due only to the shear of gas by the solids as they slip through the gas stream seems reasonable because of the low concentration of solids by volume in the gas stream. Thus the pressure loss equation developed in this research work appears to be far superior to any theoretical development in past work. Instead of using a standard air velocity of 3,000 ft/min, a mythical number introduced from other industries, the fundamental equations for solids slip have been used along with experimentally determined drag coefficients. Thus the approach to calculating gas volume requirements has been improved substantially, by the use of more reliable equations for pressure loss and particle slip.

In conjunction with these theoretical developments it has been shown that if the solids accumulation by volume exceed four percent, the lift of solids will be choked-off completely. This is a significant development and may explain difficulties that have occurred in many gas drilling operations where the removal of solids ceases suddenly. It has been shown by the use of pressure profiles that a prelude to the loss of solids lift would be the removal of solids by a slugging action.

Of particular significance were the field results that showed excessive gas volumes may reduce penetration rates. Also in this same work, it was shown that the point of minimum calculated annulus pressure losses were obtained in the same range of gas flow that the maximum rates of penetration were obtained. This introduces the possibility of a completely new approach in the optimum design for gas volume requirements. Based on these preliminary results it would be desirable to calculate a pressure profile versus volume rate of flow for any given rate of penetration and combination of hole and drill pipe size. The volume rate of gas flow that results in the minimum pressure loss in the annulus, would also be the rate of flow that would give the maximum rate of penetration. If subsequent field tests confirm these preliminary results, this would have a significant effect on drilling with gas. It could also affect methods for future design with any compressible circulating medium.

Drilling with low density fluids such as predetermined ratios of gas and liquids offer a considerable amount of promise in rotary drilling. The results developed in this work can be used with modification for three phase mixtures of gas, solids and liquids. With adequate technology it might be possible to introduce completely new concepts for circulating fluids in rotary drilling.

CHAPTER VII

RECOMMENDATIONS FOR FUTURE STUDY

The field test portions of this research need to be continued. More data are needed to confirm the complete validity of pressure loss equations developed in this research. More information is needed on drilling rates as a function of gas volumes. Previous information seem to indicate that penetration rates would be a maximum when annulus pressures are a minimum; this needs to be confirmed.

A reliable method needs to be developed for measuring annulus pressure losses while drilling with gas. One method might follow that outlined in Appendix C of this work. However, this introduces problems of innacuracy because of pipe dimensions that change with time. A preferable means would be the development of a method of actually measuring the annulus pressure losses while drilling. If such a method could be developed for routine use then required gas volumes could be determined experimentally for each operation. Prediction methods would be used only in the general sizing of equipment. If actual annulus measurements were possible only on a test basis, this would still serve to help increase the accuracy of prediction equations.

Theory should be extended to include the presence of water in the gas flow stream. Also the effects need to be considered where; (1) liquid is emulsified in the gas stream and (2) the gas is emulsified in the liquid stream. In any drilling operation either or both of these conditions may exist. It is possible to have different flow patterns in the drill pipe-hole annulus. Flow may be laminar close to bottom and convert to turbulence at some higher level. These conditions are poorly defined for three phase mixtures of solids, liquids and gases. In contrast to dry gas flow, the mixture will probably behave as a non-newtonian fluid. Thus more study of the fluid behavior for such systems is needed. If fluid behavior can be predicted, pressure losses can be calculated in laminar flow and the transition to turbulent flow can be forecast.

More basic information is needed on drag coefficients for solids in mixtures of gas and liquids. Methods need to be developed to predict the solids support capacity of gas-liquid systems. More information is needed on the effects of surface tension of various mixtures on solids lift.

In conjunction with these needs, standard testing procedures need to be developed. Some system of collecting field data are needed. Emphasis needs to be placed on the need for accuracy in field measurements.

These are not difficult suggestions; studies on non-newtonian fluid behavior are common. Results from these studies with modifications can be made applicable to the conditions encountered in gas drilling. Using low density drilling fluids in rotary drilling offer a potential means of saving millions of dollars in drilling costs.

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APPENDIX A

Appendix A includes the development and solution for some of the equations shown in the theoretical developments of Chapter III.

Derivation of Slip Velocity Equation

The basic slip velocity equation is derived from Newton's Second Law of Motion $F = ma$. Using a force balance on one particle falling through an infinite fluid stream gives the following relationship:

$$F = ma = \frac{M_p\, g}{g_c} - \frac{M_f\, g}{g_c} - F_D$$

For the condition where the particle falling through the fluid reaches a terminal velocity, the acceleration term, a, equals zero. Then:

$$\frac{M_p\, g}{g_c} - \frac{M_f\, g}{g_c} - F_D = 0$$

where: M_p = mass of the particle

M_f = mass of liquid displaced by the particle

g = acceleration of gravity

F_D = drag force on particle caused by friction

g_c = units conversion constant

$$C_D = \frac{\tau}{\frac{v_s^2}{2g_c}} \quad \text{and} \quad \tau = \frac{F_D}{A_s}$$

then:

$$F_D = C_D A_s \rho_f v_s^2 / 2g_c$$

Assuming the particle to be spherical in shape, $M_p = \frac{\pi D_p^3 \rho_p}{6}$

and $M_f = \frac{\pi D_p^3 \rho_f}{6}$

then: $$g \frac{\pi D_p^3}{6} (\rho_p - \rho_f) - \frac{C_D \pi D_p^2 \rho_f v_s^2}{8} = 0$$

and $$v_s^2 = \frac{8 g D_p (\rho_p - \rho_f)}{6 C_D \rho_f}$$

Since the density of gas ρ_f is negligible as compared to ρ_p the equation for slip velocity may be written as follows:

$$v_s = \left[\frac{4 g D_p \rho_p}{3 C_D \rho_f}\right]^{1/2}$$

Solution for Pressure Loss Equation (56)

$$P\,dP = \frac{S P^2}{53.3 ZT_a} (1 + 5\frac{M_p}{M_f})\,dh + \frac{7.84 (10^{-3}) SQ_s^2 T_a f\,dh}{(D_h^2 - D_d^2)^2 (D_h - D_d)}$$

Let: $$a = \frac{7.84 (10^{-3}) SQ_s^2 T_a f}{(D_h^2 - D_d^2)^2 (D_h - D_d)}$$

$$b = \frac{S}{53.3 ZT_a} (1 + 5 \frac{M_p}{M_f})$$

Then:

$$P\,dP = b P^2\,dh + a\,dh = (a + b P^2)\,dh$$

$$\int_{P_w}^{P} \frac{P}{a + b\,P^2}\, dP = \int_{o}^{h} dh$$

or

$$\frac{1}{b}\int_{P_w}^{P} \frac{P}{\frac{a}{b} + P^2} \quad dP = \int_{o}^{h} dh$$

$$\frac{1}{2b} \quad \ln \frac{P^2 + \frac{a}{b}}{P_w^2 + \frac{a}{b}} = h$$

$$\ln \left(\frac{P^2 + \frac{a}{b}}{P_w^2 + \frac{a}{b}}\right) = 2bh$$

$$\frac{P^2 + \frac{a}{b}}{P_w^2 + \frac{a}{b}} = e^{2bh}$$

$$P^2 + \frac{a}{b} = (P_w^2 + \frac{a}{b}) \quad e^{2bh}$$

$$P^2 = (P_w^2 + \frac{a}{b}) \quad e^{2bh} - \frac{a}{b}$$

$$P = \left[(P_w^2 + \frac{a}{b}) \quad e^{2bh} - \frac{a}{b}\right]^{1/2}$$

Derivation of Gas Mass Required to Lift Particles

Begin with the slip velocity equation as follows:

$$v_s = \left[\frac{4\ g\ D_p\ \rho_p}{3\ C_D\ \rho_f}\right]^{1/2}$$

Also: $v_s = v - v_p$

When $v_p \rightarrow 0$, $v_s \rightarrow v$, this is the minimum required fluid velocity to support particles. In this case v_p is defined as the net upward velocity of particle. Using these conditions, the slip velocity equation can be modified as follows:

$$\rho_f\ A\ v_f = \rho_f\ A\ \left[\frac{4\ g\ D_p\ \rho_p}{3\ C_D\ \rho_f}\right]^{1/2}$$

Let $G = \rho_f\ A\ v_f$ = mass of flowing gas

$$G = A\left[\frac{4\ g\ D_p\ \rho_p\ \rho_f}{3\ C_D}\right]^{1/2}$$

Substitute: $A = \frac{\pi}{4}(D_h^2 - D_d^2)$, $\rho_f = \frac{SP}{53.3\ T}$ and $\rho_p = 156\ lb_m/ft^3$

then: $$G = 8.8\ (D_h^2 - D_d^2)\left[\frac{S\ D_p\ P}{C_D\ T_a}\right]^{1/2}$$

APPENDIX B

EXAMPLE SOLUTIONS

In the theoretical developments of Chapter III, statements have been made on why certain conditions are considered important or why certain assumptions are reasonable. These statements are substantiated in the following examples.

Example B-1: Length of the zone of solids accumulation at the top of the drill collars.

Assume: Hole size = 8.75 inches

Drill pipe size = 5.00 inches

Drill collar size = 6.75 inches

v_o = 25 ft/sec

v_i = 41.5 ft/sec

Determine: h, the length of the zone in which formation solids may accumulate. Using Equation (27),

$$h = \frac{v_i^2 - v_o^2}{2g}$$

$$h = \frac{(41.5)^2 - (25)^2}{64.4} = 17.1 \text{ ft.}$$

The length of the zone of solids accumulation for the conditions assumed is 17.1 feet. If oversized drill collars, such as 8 inches O.D. were used the length of this solids accumulation zone would be 155 feet and the calculation would follow the same pattern shown in Example B-1.

Example B-2, The effect of kinetic energy on pressure loss in the drill string-hole annulus.

Assume: Conditions at the top of the drill collars in the annulus as follows:

$P = 64.7$ psia

$T_a = 600^{o}R$

$Z = 1.0$

Surface conditions are as follows

$P = 14.7$ psia

$T_a = 600^{o}R$

then: $$\Delta p = \frac{\rho_a}{g_c} \int_{v_i}^{v_o} v \, dv = \frac{\rho_a}{2g_c} (v_o^2 - v_i^2)$$

$$\Delta p = \frac{(0.202)\ (2325 - 120)}{(64.4)\ (144)} = 0.0481 \text{ psia}$$

It can be seen that even with substantially higher changes in pressure that the kinetic energy term is negligible.

Example B-3, A comparison of the order of magnitude of $M_p \rho_f$ with $M_f \rho_p$.

Assume: Required gas volume = 600 SCF/min

Drilling rate = 100 ft/hr

$\rho_f = 0.0764\ lb_m/ft^3$

$\rho_p = 156\ lb_m/ft^3$

Determine: $M_p\ \rho_f$ and $M_f\ \rho_p$

$$M_p \rho_f = \frac{(\pi)(16)(100)(156)(0.0764)}{(144)(3600)} = 0.116 \ lb_m^2/ft^3 sec$$

$$M_f \rho_p = \frac{(600)(0.0764)(156)}{60} = 119 \ lb_m^2/ft^3 sec$$

This calculation shows the order of magnitude ratio of $M_f \rho_p$ to $M_p \rho_f$ to be a 1000 to 1. Since annulus pressure losses are generally less than 100 psia, the neglect of $M_p \rho_f$ will introduce a negligible difference in results.

APPENDIX C

CALIBRATION AND METHODS FOR MEASURING ANNULUS PRESSURE LOSS IN FIELD TESTS

One complete set of pressure tests were run with the drill pipe suspended just above bottom at approximately 3,800 feet. During these tests pressures were measured with calibrated gauges at the standpipe on the rig floor, just above the bit inside the drill collar string and in the drill pipe-hole annulus just above the bit. These results were used as calibration tests since it was not possible to measure the annulus pressure just above the bit while drilling. This was true because the side pocket tool could not be used with compression in the drill string.

The measured annulus pressure loss versus the calculated annulus pressure loss using the writers theoretical equation while circulating air only is shown in Fig. C-1. The calculated pressure loss is an average of 1.0 psia less than the measured pressure loss at all flow rates.

Methods used to determine pressure losses in the various parts of the circulating system are given as follows:

Surface Connections--Between the Standpipe and Bottom of the Kelly

$$dP = \rho \, dh + \frac{\rho v^2 f}{2 g_c D} \, dh$$

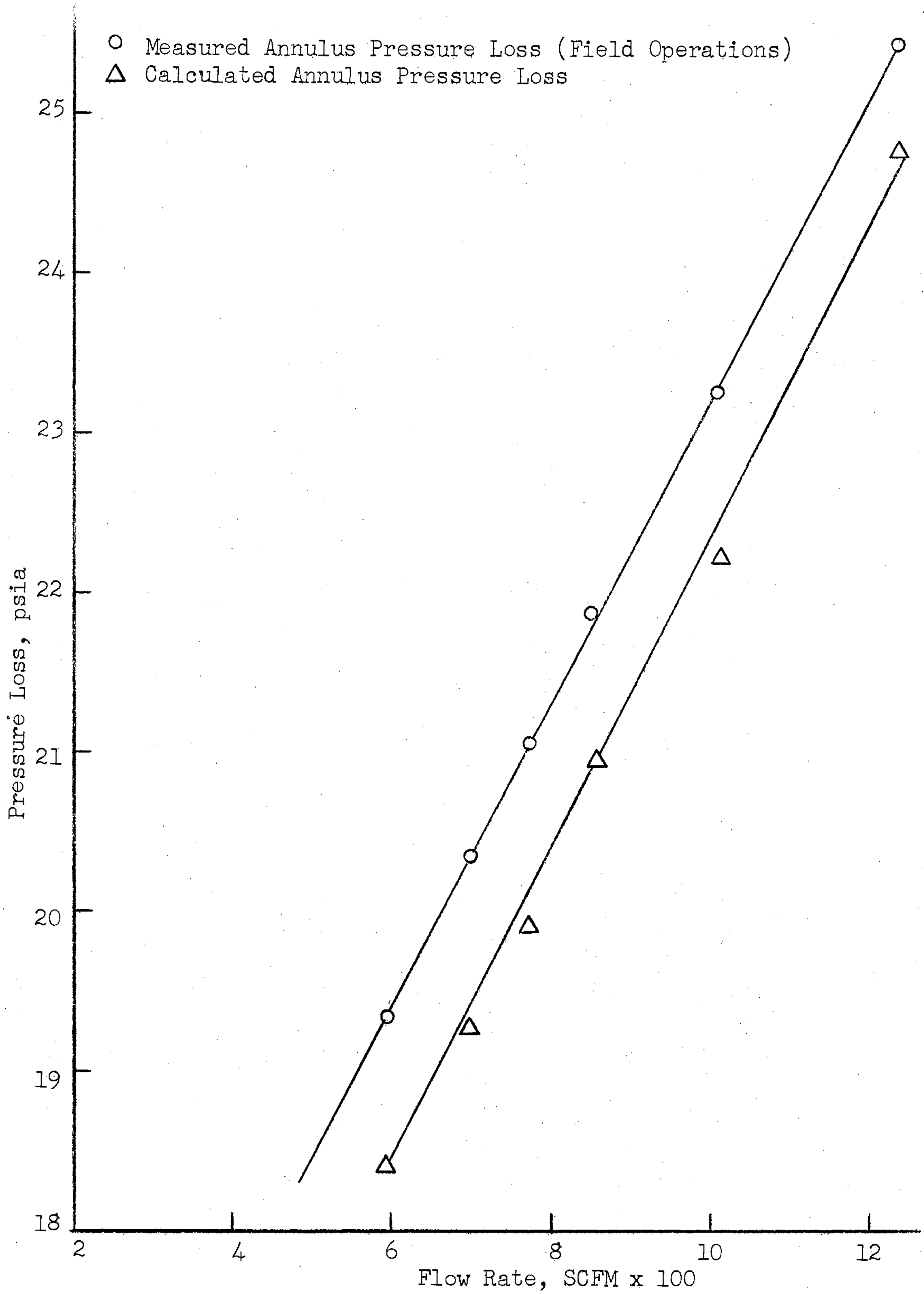

Fig. C-1. Pressure Loss vs. Air Flow Rate

For this system $\rho \, dh = 0$

$$dP = \frac{\rho v^2 f}{2 g_c D} \, dh$$

$$v = \frac{Q}{A} = \frac{(14.7) \, T_a \, Q_s}{520 \, P \, A} = \frac{0.0282 \, T_a}{P \, A} \quad Q_s$$

$$\int_{P}^{P_k} P \, dP = C' \, Q_s^2 \, T_a \int_{o}^{h} dh$$

$$P_k^2 - P_p^2 = C' \, Q_s^2 \, T \, h$$

This shows that a plot of $P_k^2 - P_p^2$ versus $Q_s^2 \, T$ will be a straight line with a slope equal to $C'h$. Actual measurements showed this correlation to be valid and Fig. C-2 was constructed to be used as a calibration curve.

Drill Pipe and Drill Collar Pressure Losses

These losses were calculated from just below the bottom of the kelly to just above the bit. A modified form of the energy balance was used for the calculations. Because the length of the drill string was changing, one calibration curve could not be used for determining pressure losses inside the pipe. Thus the energy balance was arranged in the following form and solved numerically to increase accuracy.

$$dP = \rho_f \, dh - \frac{\rho_f \, v^2 \, f}{2 g_c \, D} \, dh$$

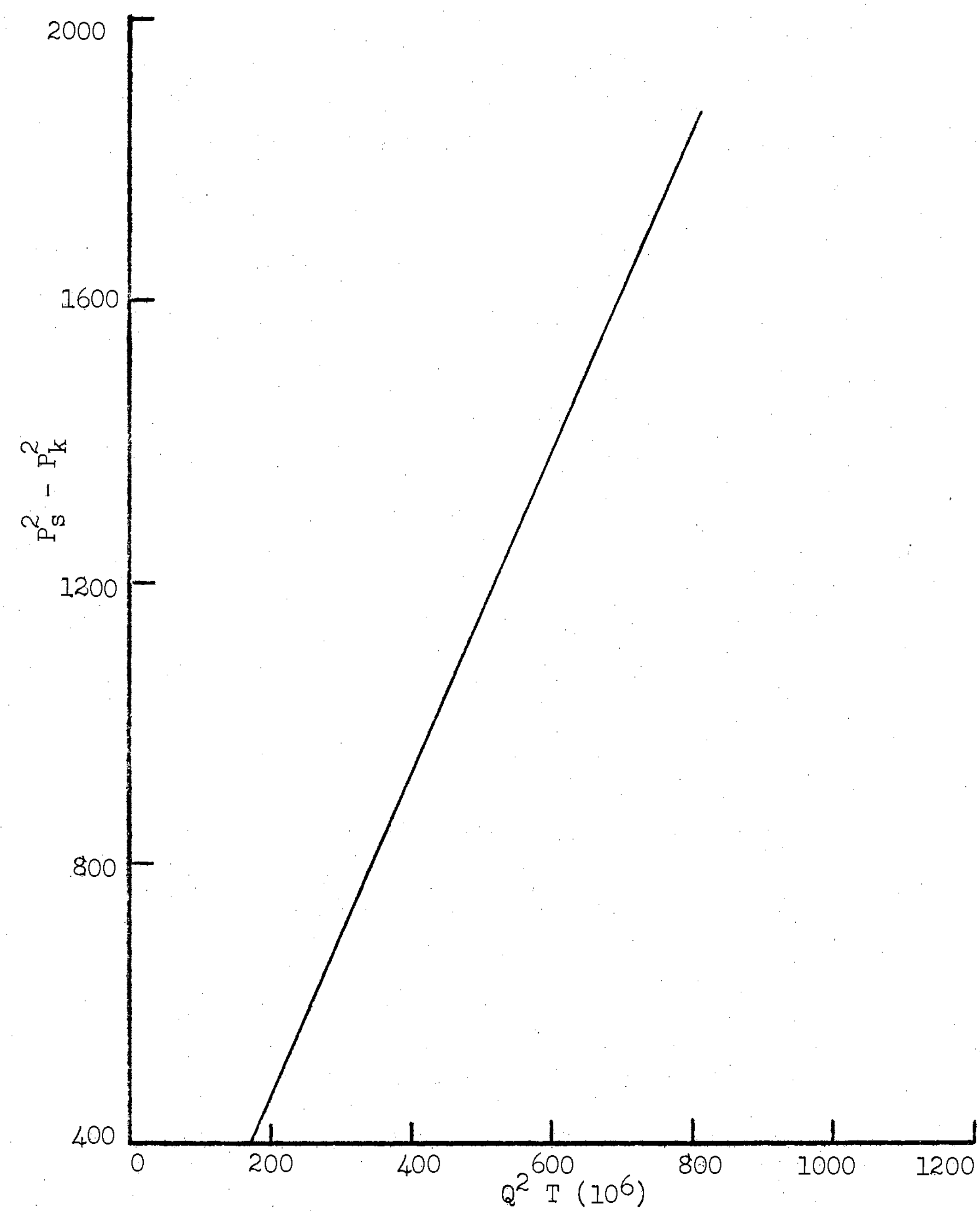

Fig. C-2. Correlation Curve for Surface Connections where: $\rho = \frac{S\ P}{R\ T}$ and $v = \frac{0.0282\ T\ Q_s}{P\ A}$

then

$$dP = \left[\frac{S\ P}{R\ T} - \frac{S\ P}{R\ T} \left(\frac{0.0282\ T}{PA} \right)^2 \frac{Q_s^2\ f}{2g_c D} \right] dh$$

$$dP = \left[\frac{S\ P^2\ A^2\ 2g_c\ D - (0.0282\ T\ Q_s)^2\ S\ f}{A^2\ R\ T\ 2g_c\ D\ P} \right] dh$$

Let: $a = 2g_c\ A^2\ D\ S$

$b = (0.0282\ T\ Q_s)^2\ S\ f$

then: $dP = \left[\frac{a\ P^2 - b}{a\ P\ R\ T} \right] dh$

$$\frac{a\ P}{a\ P^2 - b}\ dP \cong \frac{1}{R\ T}\ dh$$

This form of the equation can be written as follows:

$$\frac{a}{a\ P^2 - b}\ d(P^2) = \frac{2}{R\ T}\ dh$$

thus:

$$\frac{d(P^2)}{dh} \cong \frac{2(a\ P^2 - b)}{a\ R\ T} \cong \frac{2}{R\ T}(P^2 - \frac{b}{a})$$

Equation B-1 gives the slope of any point on a curve of P^2 versus depth h. It can be modified for numerical application as follows: For given short intervals,

$$\frac{d(P^2)}{dh} \qquad \frac{\Delta(P^2)}{\Delta h}$$

then:

$$P_2^2 - P_1^2 = \frac{d(P^2)}{dh}(h_2 - h_1)$$

Using this calculation, a typical profile through the drill pipe and drill collars was constructed as shown in Fig. C-3. An example calculation is illustrated as follows:

Well Depth: 5,038 feet

Surface Pressure (just below kelly) = 170 psia

q_s = 1189.11 SCFM

T = 518°R

$$\frac{d(P^2)}{dh} = \frac{2}{(53.3)\ (518)} = \frac{P^2 - 1.384\ (10^{-3})\ (1189 \times 518)^2\ (0.023)}{(2.764)^5}$$

$$\frac{d(P^2)}{dh} = -4.74$$

Let Δh = 100 and calculate the pressure at the next point down the pipe.

$$P_2^2 = P_1^2 + \frac{d(P^2)}{dh}\ \Delta h$$

$$P_2^2 = 28.900 - 4.74\ (100) = 28,426$$

$$P_2 = 168.6 \text{ psia}$$

From this another interval is selected and the next P_2 is calculated using 168.6 psia as P_1.

Pressure Drop Across Bit

Pressure losses through the bit were determined using the conventional flow through an orifice formula used in A.G.A. Report No. 3. This formula and the method in which it was utilized is shown as follows:

$$Q_s = 129.226\ E\ D_2^2 \left[\frac{h_w\ P}{T}\right]^{1/2}$$

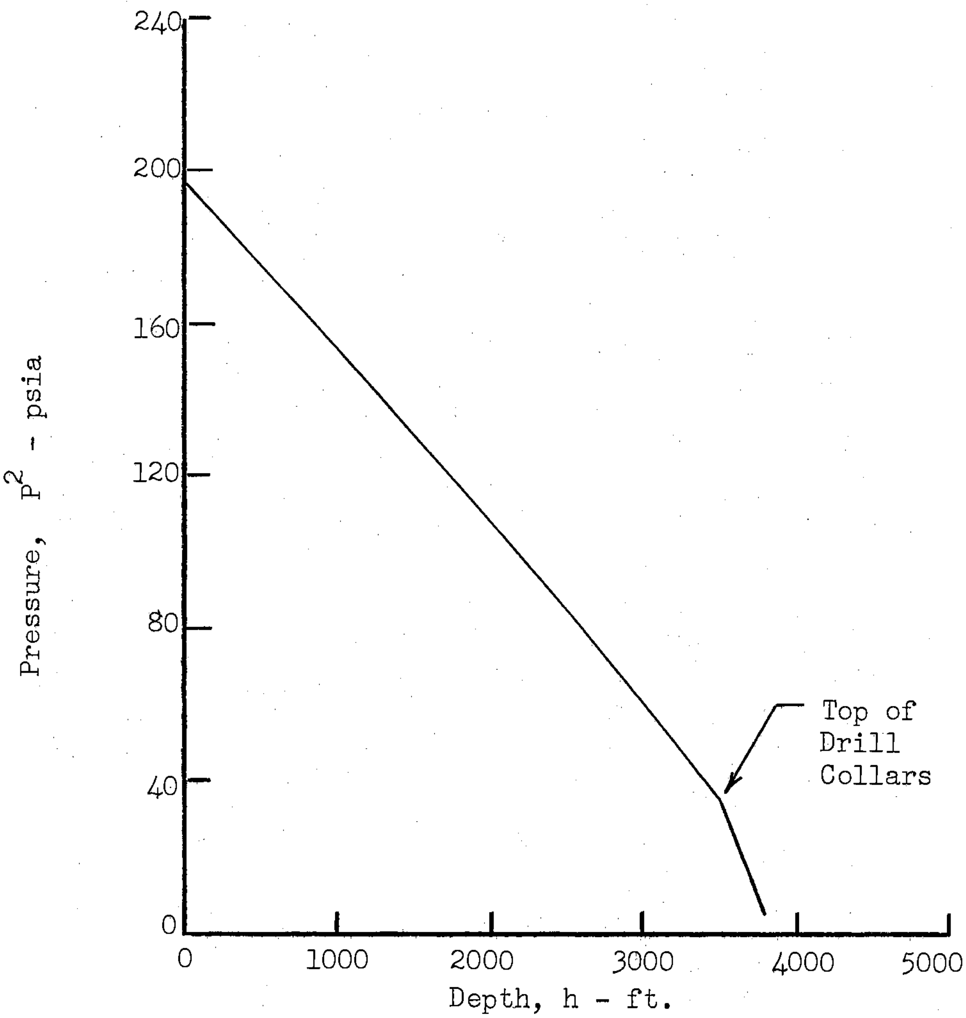

Fig. C-3. Pressure Variations with Depth in
3-1/2 in. Drill Pipe

Let $C_1 = 129.226\ E\ D_2^2$

Then:

$$Q_s = C_1 \left[\frac{h_w\ P}{T} \right]^{1/2}$$

Because pressure drop is a function of the fluid head, h_w can be expressed as follows:

$$h_w = C_2\ (P_i - P_o)$$

$$\frac{Q_s}{C_1} = \left[\frac{C_2\ (P_i - P_o)\ P_i}{T} \right]^{1/2}$$

$$\frac{Q_s^2}{C_1^2} = \frac{C_2\ (P_i - P_o)\ P_i}{T}$$

$$P_i - P_o = \frac{1}{C_2 C_1^2} \left[\frac{Q_s^2\ T}{P_i} \right] \qquad (4)$$

Based on the form of Equation (4), a plot of $P_i - P_o$ versus $\frac{Q_s^2\ T}{P_i}$ will give a straight line with a slope of $\frac{1}{C_2 C_1^2}$. Such a plot has been prepared from measured data and this is shown in Fig. C-4.

A summary of the data used to determine annulus pressure loss is shown in Table C-1.

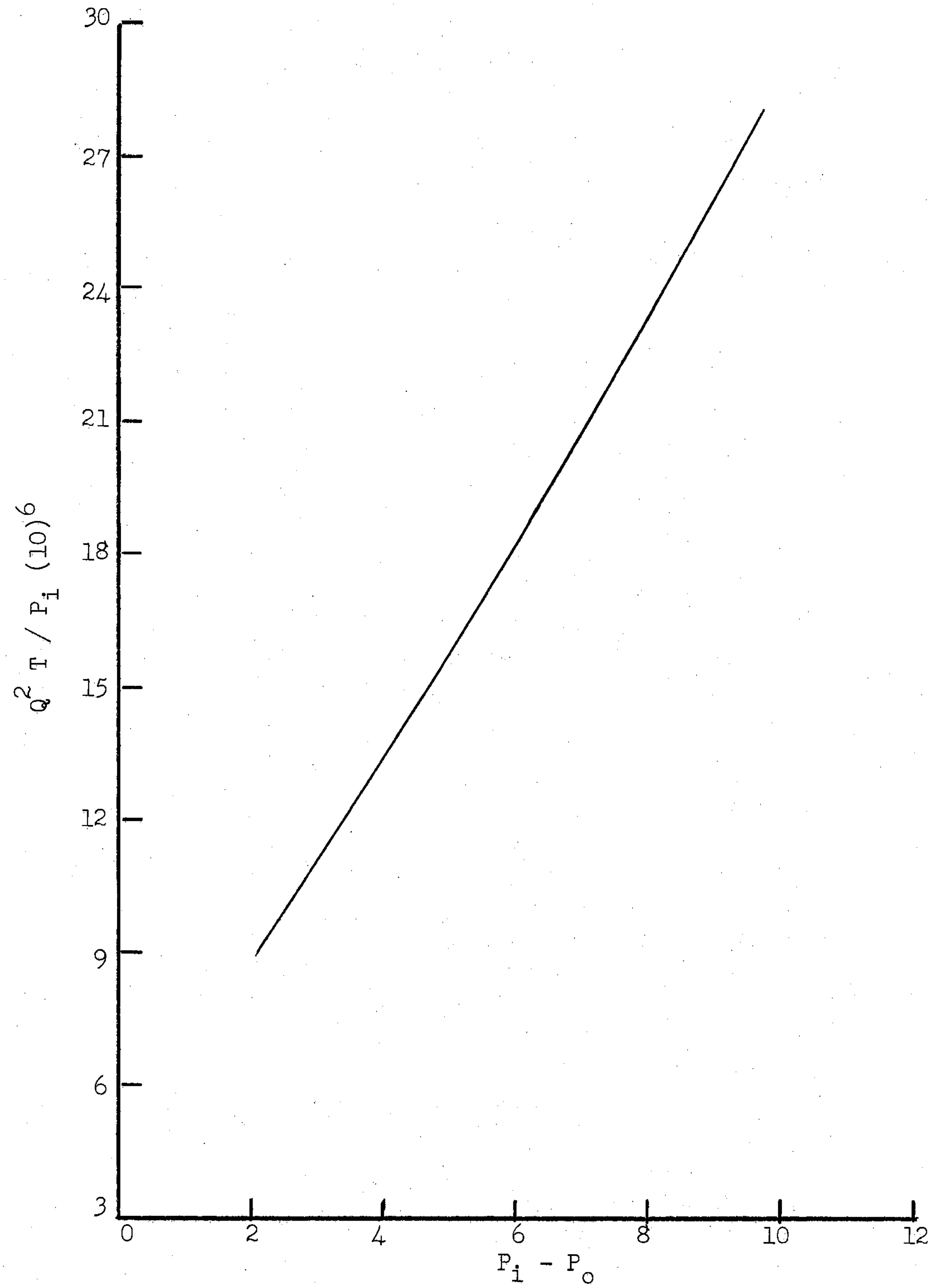

Fig. C-4. Correlation Curve for Drill Bit

TABLE C-1

SUMMARY OF CALCULATIONS TO DETERMINE ANNULUS PRESSURE LOSSES

Well Depth feet	Flow Rate SCFM	Pressure Above Bit psia	Temp. °R	Q^2T/P_i From. Fig. C-4	Pressure Loss Through Bit, psia	Pressure Loss in Annulus psia
3,812	659	32.06	591	8.013	1.62	30.44
	733	34.05	591	9.337	2.23	31.82
	869	37.73	591	11.831	3.31	34.41
	1028	41.92	591	14.897	4.60	37.32
	1215	46.84	591	18.640	6.13	40.71
4,488	571	30.35	608	6.526	0.95	29.40
	678	32.28	608	8.659	1.91	30.37
	752	33.48	608	10.264	2.63	30.85
	825	34.62	608	11.988	3.38	31.24
	1001	37.05	608	16.441	5.25	31.80
	1179	39.21	608	21.56	7.28	31.93
5,038	584	38.34	624	5.55	0.51	37.83
	698	39.91	624	7.62	1.43	38.48
	761	40.56	624	9.61	2.35	38.21
	1008	42.14	624	15.04	4.67	37.41
	1189	42.84	624	20.86	7.00	35.28
5,619	593	43.85	644	5.17	0.35	43.50
	709	45.65	644	7.09	1.08	44.57
	805	46.72	644	8.94	2.05	44.67
	877	47.29	644	10.47	2.73	44.56
	1043	47.97	644	14.88	4.60	43.37

APPENDIX D

Comparisons with other investigators' experimental data;

$$\frac{dP}{dh} = \rho_f + 5\rho_f \frac{M_p}{M_f} + \frac{\rho_f v^2 f}{2 g_c (D_h - D_d)} \qquad (52)$$

ρ_f = the density of the flowing medium, for the test conditions used by Williams, 80°F and 15.5 psia, the air density is determined as follows:

$$\rho_f = \frac{PM}{RT} = \frac{(15.5)(28.96)}{(10.72)(540)} = 0.078 \ lb_m/ft^3 = 5.41 \ (10^{-4}) \ lb_m/ft\text{-}in^2$$

Williams used a constant sand feed rate, which means the unit mass of particles per unit mass of air M_p/M_f varied relative to the air flow rate. The M_p/M_f ratio was determined as follows:

$$\text{Feed Rate} = \frac{14 \ lb_m \ \pi \ (1.05)^2 \ in^2 \ ft^2}{ft^2\text{-}sec \ (4) \ 144 \ in^2} = 0.0842 \ lb_m/sec$$

$$M_p/M_f = \frac{0.0842 \ lb_m/sec}{\rho \ Q(air) \ lb_m/sec} = \frac{0.0842 \ lb_m \ sec(4) \ \ in^2\text{-}ft}{v \ ft\text{-}sec \ \pi(1.05)^2 in^2 \ 5.4 \ (10^{-4}) \ lb_m}$$

$$M_p/M_f = \frac{179.5}{v}$$

$$\text{Feed Rate} = \frac{35 \ lb_m \ \pi \ (1.05)^2 in^2 ft^2}{ft^2\text{-}sec(4) \ 144 \ in^2} = 0.2104 \ lb_m/sec$$

$$M_p/M_f = \frac{0.2104 \ lb_m/sec}{\rho \ Q(air) \ lb_m/sec} = \frac{0.2104 \ lb_m \ sec(4) \ in^2\text{-}ft}{v \ ft \ sec \ \pi(1.05)^2 in^2 \ 5.41(10^{-4}) lb_m} = \frac{449}{v}$$

Friction factor used:

$$f = .014\ (D)^{-1/3} = \frac{(0.14)}{\left[\frac{(1.05)}{12}\right]^{1/3}} = 0.0315$$

Feed Rate = 14 lb_m/ft^2-sec

$$\frac{dP}{dh} = \frac{5.41(10^{-4})lb_m}{ft\text{-}in^2}\left[1 + \frac{5(179.5)}{v}\right] + \frac{.0315(5.41)(10^{-4})lb_m lb_f\text{-}sec^2}{64.4\ lb_m\text{-}ft\ ft\text{-}in^2\ 0.0875}\ \frac{v^2}{ft}\ \frac{ft^2}{sec^2}$$

$$\frac{dP}{dh} = 5.41(10^{-4}) + \frac{.486}{v} + 3.02\ (10^{-6})v^2 \qquad \text{(D-1)}$$

Feed Rate = 35 lb_m/ft^2-sec

$$\frac{dP}{dh} = \frac{5.41\ (10^{-4})lb_m\ 32.2\ ft\ lb_f\text{-}sec^2}{ft\text{-}in^2\ sec^2\ 32.2\ lb_m\text{-}ft}\left[1 + \frac{5\ (449)}{v}\right] +$$

$$\left[\frac{.0315\ (5.41)(10^{-4})\ lb_m}{64.4\ lb_m\text{-}ft\ ft\text{-}in^2}\right]\left[\frac{lb_f\text{-}sec^2\ v^2\ ft^2}{.0875\ ft\ sec^2}\right]$$

$$\frac{dP}{dh} = 5.41\ (10^{-4}) + \frac{1.2}{v} + 3.02\ (10^{-6})\ v^2 \qquad \text{(d-2)}$$

The solutions of Equations (D-1) and (D-2) are given in Table D-1.

TABLE D-1

PRESSURE LOSS VS. AIR VELOCITY

Air Velocity		Pressure Loss, psia/ft		
ft/ sec.	ft/ min.	Air Only	Sand Rate 14 lbm/ft^2-sec	Sand Rate 35 lbm/ft^2-sec
30	1800	.003259	.019459	.043259
60	3600	.011413	.019513	.031413
80	4800	.019869	.025944	.034869
100	6000	.030741	.035601	.042741
120	7200	.043488	.047538	.053488
140	8400	.059192	.062662	.067762

NOMENCLATURE

a - Acceleration, ft/sec^2

A - Flow area, square feet

A_a - Annulus area, square feet

A_s - Shear area of one particle, square feet

C_D - Drag coefficient, dimensionless

D - Internal pipe diameter, feet

D_a - average particle diameter, feet

D_d - Outside pipe diameter, feet

D_e - Equivalent particle diameter, feet

D_h - Hole diameter, feet

D_p - Particle diameter, feet

F - Force, lb_f

F_D - Drag force, lb_f

f - Moody friction factor, dimensionless

g - Acceleration of gravity, 32.2 ft/sec^2

g_c - Units conversion constant, 32.2 $lb_m\text{-}ft/lb_f\text{-}sec^2$

G - Mass flow rate of gas, lb_m

h - Well depth, feet

$\overline{H}_i$ - Enthalpy into selected system, $ft\text{-}lb_f/lb_m$

$\overline{H}_o$ - Enthalpy out of selected system, $ft\text{-}lb_f/lb_m$

$\overline{KE}_i$ - Kinetic energy into selected system, $ft\text{-}lb_f/lb_m$

$\overline{KE}_o$ - Kinetic energy out of selected system, $ft\text{-}lb_f/lb_m$

M - Mass, lb_m

M_p - Mass of particles, lb_m

M_f - Mass of fluid, lb_m

n - Ratio of specific surface areas

p - Pressure at any point, psia

P - Pressure at any point, psfa

P_p - Pressure loss due to particles or solids, psfa

P_s - Standard pressure, psfa

P_w - Well-head pressure, psfa

P_1, P_2 - Pressure at a specific point, psfa

$\overline{PE}_i$ - Potential energy into selected system, ft-lb_f/lb_m

$\overline{PE}_o$ - Potential energy out of selected system, ft-lb_f/lb_m

ΣQ - Heat transferred at boundaries of system, ft-lb_f/lb_m

Q_s - Volume flow rate of gas, SCFM

r - Drilling Rate, ft/hr

R_e - Fluid Reynolds number, dimensionless

R_p - Particle Reynolds number, dimensionless

S - Specific gravity of gas, dimensionless

$\Delta \overline{S}$ - Entropy change into and out of system, ft-lb_f/lb_m

$\overline{S}_p$ - Entropy produced within system, ft-lb_f/lb_m

t - Time, second

T - Temperature at any point, ^oR

T_a - Average temperature of flow stream, ^oR

T_s - Standard temperature, ^oR

v - Fluid velocity, ft/sec

v_a - Actual slip velocity, ft/sec

v_c - Choking velocity, ft/sec

v_p - Net upward particle velocity, ft/sec

v_s - Theoretical slip velocity at any point, ft/sec

v_{ss} - Theoretical slip velocity at standard conditions, ft/sec

$\bar{v}_i$ - Velocity into selected system, ft/sec

$\bar{v}_o$ - Velocity out of selected system, ft/sec

W - Solids feed rate, $lb_m/sec\text{-}ft^2$

x - Sphericity or shape factor

ρ - Density, lb_m/ft^3

ρ_f - Fluid density, lb_m/ft^3

ρ_p - Particle density, lb_m/ft^3

ρ_m - Fluid-particle mixture, density. lb_m/ft^3

ρ_s - Fluid density at standard conditions, lb_m/ft^3

μ - Fluid viscosity, centipoise

ϵ - Voidage fraction, dimensionless

ft - feet

lb_f - pounds force

lb_m - pounds mass

psia - pounds per square inch absolute

psfa - pounds per square foot absolute

SCFM - Standard cubic feet per minute

^oR - Degrees Rankine

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