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Experimental evaluation of control fluid fallback during off-bottom well control in vertical and deviated wells

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EXPERIMENTAL EVALUATION OF CONTROL FLUID FALBACK DURING OFF-BOTTOM WELL CONTROL IN VERTICAL AND DEVIATED WELLS

A Dissertation

Submitted to the Graduate Faculty of the Louisiana State University and Agricultural and Mechanical College in partial fulfillment of the requirements for the degree of Doctor of Philosophy

in

The Craft and Hawkins Department of Petroleum Engineering

by

Fernando Sebastian Flores-Avila
B.S., Universidad Nacional Autonoma de México, 1986
M.S., Universidad Nacional Autonoma de México, 1998
May 2002
DEDICATION

I wish to dedicate this work to my lovely wife Adela because she has been there when I needed most, giving me support and encourage reaching our goals. For all her love and patience in those endless hours of study. Once again, she has showed me that being together is the most important fact, and together we will keep building our future and the future of our girls.

Also I would like to dedicate this work to my children:

Jesus Fernando (✝)
Maria Fernanda (Maryfer).
Maria Adela (Adelita).
Camila (Cami).

For all the happiness and strength that you always give us, for being the source of love and energy in our lives. Thank you girls for all the patience and love to “daddy.”

Last but not least I dedicate this work to God. I thank him for giving me the opportunity and strength to do my studies.
ACKNOWLEDGMENTS

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NOMENCLATURE

\[ A_L \] Average flow area for liquid (ft\(^2\)).

\[ A_p \] Element area (ft\(^2\)).

\[ C \] Empirical constant

\[ d_i \] Pipe ID. (ft)

\[ d_m \] Droplet diameter (ft)

\[ d_{\text{max}} \] Maximum droplet size (ft)

\[ f \] Fanning friction factor

\[ F \] Factor to account for effect of turbulence on \( K_d \).

\[ F_p \] Pressure correction factor

\[ g \] Acceleration of gravity, 32.2 ft/sec\(^2\).

\[ g_c \] Critical acceleration of gravity, 32.2 lbm-ft/lbf-sec\(^2\).

\[ H_G \] Gas holdup or void fraction.

\[ H_{G0} \] Gas holdup at ZNLF conditions.

\[ H_L \] Liquid holdup.

\[ H_{L0} \] ZNLF holdup.

\[ K_d \] Drag coefficient for spheres.

\[ L \] Tubing length in DP cell (ft).

\[ L_d \] Pipe length in annular flow (ft).

\[ L_p \] Pipe length (ft).

\[ N_{Re} \] Reynolds number.

\[ N_{we} \] Weber Number.

\[ p \] Pressure (psia).
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>$Q_g$</td>
<td>Gas flow rate (scf/min).</td>
</tr>
<tr>
<td>$Q_L$</td>
<td>Liquid Flow Rate (ft³/min).</td>
</tr>
<tr>
<td>$q_{gsc}$</td>
<td>Gas flow rate at standard conditions (MMscf/D).</td>
</tr>
<tr>
<td>$T$</td>
<td>Temperature ($^\circ$F)</td>
</tr>
<tr>
<td>$\bar{V}$</td>
<td>Average velocity of continuous phase (ft/sec).</td>
</tr>
<tr>
<td>$V_L$</td>
<td>Liquid volume (ft³).</td>
</tr>
<tr>
<td>$V_P$</td>
<td>Element volume (ft³).</td>
</tr>
<tr>
<td>$v_g$</td>
<td>Gas velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_{G0}$</td>
<td>Gas slip velocity at ZNLF (ft/sec).</td>
</tr>
<tr>
<td>$v_L$</td>
<td>Liquid velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_m$</td>
<td>Mixture velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_s$</td>
<td>Slip velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_{Scrit}$</td>
<td>Critical gas velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_{SG}$</td>
<td>Superficial gas velocity (ft/sec).</td>
</tr>
<tr>
<td>$v_{SL}$</td>
<td>Superficial liquid velocity (ft/sec).</td>
</tr>
<tr>
<td>$z_p$</td>
<td>Gas compressibility factor at average pressure.</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>Deviation angle from vertical (Degrees)</td>
</tr>
<tr>
<td>$\alpha_{real}$</td>
<td>Deviation angle from vertical (Degrees)</td>
</tr>
<tr>
<td>$\alpha_{equ}$</td>
<td>Equivalent deviation angle from vertical (Degrees)</td>
</tr>
<tr>
<td>$\phi$</td>
<td>Percent of liquid carryover.</td>
</tr>
<tr>
<td>$\lambda_L$</td>
<td>Non slip liquid holdup.</td>
</tr>
</tbody>
</table>
\[ \rho_c \] Density continuous phase (lbm/ft\(^3\)).

\[ \rho_g \] Gas density (lbm/ft\(^3\)).

\[ \rho_G \] Gas density (ppg).

\[ \rho_l \] Liquid density (lbm/ft\(^3\)).

\[ \rho_L \] Liquid density (ppg).

\[ \rho_s \] Liquid-gas mixture density (ppg).

\[ \rho_w \] Water density (ppg).

\[ \sigma \] Surface tension (lbf/ft).

\[ \tau_i \] Interfacial shear stress (lbf/ft\(^2\)).

\[ \tau_o \] Shear stress at pipe wall (lbf/ft\(^2\))

\[ \mu_c \] Continuous phase viscosity (lbm/ft-sec).

\[ \mu_d \] Disperse phase viscosity (lbm/ft-sec).
ABSTRACT

This study measured the liquid fallback during simulated blowout conditions. The purpose of the study was to establish a basis for developing a procedure for controlling blowouts that relies on the accumulation of liquid kill fluid injected while the well continues to flow. The results from experiments performed with air, water, 10.5 ppg and 12.0 ppg mud in an experimental 48 ft flow loop at 0°, 20°, 40°, 60° and 75° deviation angles from the vertical, as well as results from full-scale experiments performed with natural gas and water based drilling fluid in a vertical 2787-foot deep research well, are presented. The results show that the critical velocity that prevents control fluid accumulation can be predicted by Turner’s model of terminal velocity based on the liquid droplet theory by also considering the flow regime of the continuous phase when evaluating the drag coefficient, and the angle of deviation from the vertical. Similarly, the amount of liquid that flows countercurrent into and accumulates in the well can be predicted based on the concept of zero net liquid flow (ZNLF) holdup. Finally all these concepts are integrated in the dynamic kill procedure, which is based on system performance analysis to better predict the feasibility of an off-bottom dynamic kill.
CHAPTER 1

INTRODUCTION

Most of the energy supply for today’s world comes from hydrocarbons, which are produced from oil and gas reservoirs through wells, which provide the communication path between the subsurface structures to the surface where they are transported and processed for their usage. The oil and gas industry will continue to drill more and more wells to produce the hydrocarbons required to satisfy the current and future demand. As long as there are wells being drilled, there will always exist the potential risk of a blowout.

In drilling and workover operations, it is important to maintain control of the pressure in the well with the drilling or completion fluid, so that the pressure exerted by the hydrostatic column will keep the formation fluid in its place. This hydrostatic column should not exceed the fracture pressure of the formation.

If this hydrostatic pressure caused by the control fluid is less than the formation pressure, formation fluids will flow into the well causing what it is call a kick. If this kick is not controlled properly, formation fluids can flow violently through the well up to the surface without control, causing a blowout. An underground blowout is any similarly uncontrolled flow of formation fluids from one subsurface formation to another. Both of these represent a serious problem financially and environmentally. They are dangerous and destructive and may result in the loss of human lives, among other consequences. Despite success from improved technology and training, blowouts, and underground blowouts still occur.

There have been cases in which the drill pipe is partially removed from the well when the blow out occurs, or in other cases when the drill pipe suffers severe damage. In these instances, it is not possible to perform a conventional well control procedure, because the kill fluids cannot be circulated through a flow conduit from the surface to a point near the depth of formation fluid entry. Such conditions demand “off bottom” well control or kill procedures (Figure 1.1).

![Figure 1.1 Off Bottom Dynamic Kill (Bourgoyne et al., 1994)](image-url)
For the specific case of the off bottom dynamic kill, the tendency for the dense kill fluid to fall through, and flow counter-current to the formation fluid flow out of the well, can provide a means of killing the well even if the typical design criteria cannot be met. However, this counter-current flow process is poorly understood, and no design or prediction methods currently exist for a kill procedure using this concept.

Conventional well control techniques require that several basic conditions have to be met to allow a constant bottom hole pressure while circulating during the control, and these are:

- The workstring should be on or near bottom.
- The well has been planned and designed properly.
- The surface equipment and casings can contain the kick.
- No leak-off or loss of circulation occurs.

The three common conventional methods (Abel et al., 1994) are:

- Driller’s method.
- Wait and weight method.
- Concurrent method.

Unfortunately many times when these blowouts happen, the drill string is not located at the bottom of the well, making the control operation more difficult, as the conventional well control techniques cannot be applied due to the lack of a flow conduit to the bottom of the well that would allow the control or kill fluid reach the bottom. “Reviewing serious well control events (blowouts and fires) on drilling and workover wells, one will find that more than 80% first encountered problems while the work string was off bottom” (Abel et al., 1994). The three most common non-conventional methods are:

- Volumetric / Lubrication.
- Bullheading.
- Dynamic kill.

Volumetric / lubrication and bullheading, are most applicable to kicks that have been successfully shut in at the surface, while the dynamic kill is most applicable to surface and underground blowouts. In particular, the “Dynamic Kill” technique has been used to regain control of blowouts which occurred during off bottom operations. The most common design method for dynamic kills assumes that no kill liquid falls in to the well counter-current to the formation fluid that is flowing upwards. If this conservative assumption indicates that the kill is possible to achieve, the operator can proceed with the field operations with more confidence. However in some cases, calculations under this assumption will indicate that the kill is not possible, discarding a valuable potential solution to the problem. Therefore counter-current flow of kill fluid falling through formation fluid that is flowing upward is a subject that should be
studied and evaluated for a better understanding and development of off bottom dynamic kill procedures. Prior research on this phenomena is relatively limited and is described in chapter 2.

This study presents the results of an experimental investigation performed in the LSU #1 well, and a special circulating flow loop at the Petroleum Engineering Research and Technology Transfer Laboratory (PERTTL) at Louisiana State University. The phenomenon of counter-current flow of kill fluid through formation fluid flowing upwards in a blowout well, and its relation to the dynamic kill method is studied. The critical gas velocity for complete control fluid removal was evaluated experimentally, and compared with the different models available in the literature, based on the liquid droplet theory. A solution approach to the problem of evaluating the amount of liquid that flows in countercurrent and accumulates at the bottom of the well based on the concept of zero net liquid flow (ZNLF) holdup is also presented. Finally, an enhanced well control method for off bottom dynamic kills is introduced. It is based on a system performance analysis involving the parameters studied in this research (critical gas velocity and ZNLF holdup) as variables for the design and establishes the operating range to apply this enhanced method.
CHAPTER 2

LITERATURE REVIEW

The literature review in this study is divided in three sections. The first one deals with papers published in the area of critical gas velocity required to completely remove a liquid droplet in a high velocity gas core. The second one focuses on papers published evaluating ZNLF holdup, and countercurrent flow of liquid in a high velocity gas core. The final section reviews papers published addressing the problem of off bottom well control and approaches for solving the problem.

2.1 CRITICAL GAS VELOCITY

The critical gas velocity is defined as the velocity at which liquid droplets would begin to fall countercurrent to a high velocity gas core. This critical velocity is a function of the gas and liquid properties, as well as the flow characteristics of the continuous phase.

2.1.1 Jack O. Duggan (1961)

Duggan presented for the first time an empirical correlation for the gas velocity required to keep a gas well unloaded from field observations in gas wells in Texas. He established that a minimum velocity of 5 ft/sec at the wellhead will keep the well unloaded by observing the flowing performance of a number of wells having various fluid contents and producing under a wide range of operating conditions.


Turner et al. (1969) performed an analysis, and showed the existence of two proposed physical models for the removal of gas well liquids:

- Liquid film movement along the walls of the pipe.
- Liquid droplets entrained in the high velocity gas core.

Based on field data from producing gas wells, they found that the liquid droplet model better predicts the load up of gas wells producing liquids, and therefore is the governing mechanism for this process.

In the continuous film model, the transport of the liquid film in the upward direction is a result of the interfacial shear ($\tau$) of the moving gas on the surface of the liquid (Figure 2.1).

This motion is restricted by the action of gravity and wall friction. At any point $y$ distance from the wall, there exists a velocity $v$ and a shear stress $\tau$. The resisting shear stress at the wall is $\tau_o$. A steady-state force balance shows that at any point $y$,

$$\frac{\tau}{\tau_o} = 1 + \frac{y\rho_L g}{\tau_o G_c} \quad \text{(2.1)}$$
Since the interfacial shear ($\tau_i$) provides the motivating force for moving the film upward, and the gravitational shear stress, $h\rho_L g / g_c$, and the shear stress at the wall ($\tau_w$) are the resisting movement, the minimum flow condition for film movement will be when the interfacial shear ($\tau_i$) approaches the value of the gravitational shear and the shear stress at the wall ($\tau_w$) approaches zero.

Their analysis and modeling of liquid droplets entrained in the high velocity gas core has been adopted and tested successfully by many other authors and it is still one of the best to predict critical gas velocities for unloading gas wells. In this model, the terminal velocity is the maximum velocity that a particle can attain under the influence of gravity alone, when the drag forces equal the acceleration (gravitational) forces. This terminal velocity is a function of the size, shape and density of the particle and of the density and viscosity of the fluid medium. The surface tension of the liquid phase acts to draw the drop into an spheroidal shape, and the general free settling velocity equation in terms of the drop diameter is given by:

$$v_t = 6.55 \frac{d(\rho_f - \rho_g)}{\rho_g K_d}$$  \hspace{1cm} (2.2)

Finding the largest droplet size of liquid that could exist in a gas stream, was studied by Hinze (Hinze, 1945 and 1949), who showed that liquid drops moving relative to a gas are subjected to forces that try to shatter the drop, while the surface tension of the liquid acts to hold the drop together. He stated that it is the antagonism of two pressures, the velocity pressure ($v^2 \rho_g / g_c$) and the surface tension pressure ($\sigma / d$), that determines the maximum size that a drop may maintain. The ratio of these two pressures is the Weber number defined as:

$$N_{we} = \frac{v^2 \rho_g d_m}{\sigma g_c}$$  \hspace{1cm} (2.3)

Hinze showed that if the Weber number exceeded a critical value, a liquid drop would shatter. For free falling drops, the value of the critical Weber number was found to be on the
order of 20 to 30. Turner assumed the largest of these values and solved the equation for the diameter, arriving at the following expression:

\[ d_m = \frac{30 \sigma g}{\rho_g v_i^2} \quad (2.4) \]

The drop shape and the drop Reynolds number \((N_{Re})\) influence the drag coefficient. According to Lapple (Lapple 1950) the drag coefficient for spheres in the range of \(N_{Re}\) from \(10^4\) to \(10^5\), which Turner considered as typical field conditions, is relatively constant at a value of 0.44. Making all these assumptions and solving Equations 2.2 and 2.4 for the critical gas velocity \(v_t\), and making a correction of 20% to fit the field data that Turner presented, the equation proposed for the critical gas velocity is given by:

\[ v_{crit} = 20.4 \frac{\sigma^{\frac{1}{4}}(\rho_l - \rho_g)^{\frac{1}{4}}}{\rho_g^{\frac{1}{2}}} \quad (2.5) \]

2.1.3 T.N. Libson, J.R. Henry (1980)

Libson and Henry (1980) presented a case history that allowed him to identify the critical gas velocity for liquid removal in gas wells for the Intermediate Shelf Gas Play of southwest Texas. They found that for these gas wells producing from low permeability sands occurring at depths ranging from 3,000 to more than 7,500 ft, the critical unloading velocity was on the order of 1,000 ft/min, taken as a surface measurement, and not at the formation.

2.1.4 M. Ike, Ch.U. Ikoku (1981)

Ike and Ikoku (1981) developed a model to predict the minimum gas flow rate for continuous liquid removal in gas wells, including the effect of entrained liquid drops in the gas core, film thickness and pressure drop. The Duns and Ross (1963) pressure drop correlation was incorporated in this model because of its better accuracy in the annular-mist flow region. This study showed that a higher specific gravity gas is a better carrier than a lower specific gravity gas. They stated that a categorical conclusion on the accuracy of their model, cannot be made because of insufficient data, and that the model should be tested with more field data.

2.1.5 T.R. Neves, R.M. Brimhall (1989)

Neves and Brimhall (1989) presented a method to evaluate the severity of a loading problem in a gas well by using the Beggs and Brill (1973) multiphase pressure drop correlation. They proposed to calculate and plot the in-situ gas and liquid velocities at incremental depths from the surface to the bottom of the well string. The critical gas velocity profile is then calculated using Turner’s equation (Turner et al. 1969) and plotted with the previously predicted velocity profile. With this graph, one may analyze the severity of the liquid loading problem and predict the gas flow rate necessary to attain or exceed the critical gas velocity as predicted by Turner. The closer these two velocity profiles are, the nearer the well is to loading up. If the in-situ velocity profile is to the left of the critical velocity profile, the well most probably will be loaded up. They also present several remedial methods for loading in gas wells.

Coleman et al. (1991a, b, c and d) presented a series of four papers in which they discuss the results of field tests to verify the minimum flow rate or critical rate required to keep low pressure gas wells unloaded and compared their results to previous works. Some of their important conclusions are that the minimum flow rate or critical rate required to keep low pressure gas wells unloaded can be predicted adequately with Turner’s liquid droplet model without the 20% upward adjustment, but they provide no explanation of why this may occur. Also they mentioned that wells that exhibit slugging behavior might not follow the liquid-droplet model because of a different transport mechanism.

Another important conclusion reached by them is that variables such as temperature, gas and liquid gravity and interfacial tension have little effect on the critical rate, whereas wellbore diameter and pressure have a direct and significant impact. In their last paper (Coleman et al. 1991d), they presented for the first time the application of the system-network-analysis (SNA), also called Nodal System Analysis, to predict abandonment pressures for depletion drive reservoirs, and demonstrate that SNA by itself tends to underestimate the abandonment pressure.

2.1.7 A.K. Moltz (1992)

Moltz (1992) applied the same principle of Nodal System Analysis, and concluded that it can accurately predict the reservoir pressure and flow rate at the onset of load up in low-pressure gas wells when compositional wellbore fluid modeling is used. He stated that both the increasing water content of produced gas that occurs with declining reservoir pressure and the phase behavior of this water in the wellbore must be represented and included in the nodal analysis to reliably predict performance in low-pressure gas wells.

2.1.8 J.T. Boswell, J.D. Hackema (1997)

Boswell and Hackema (1997) presented a paper in which they propose continuous gas circulation as an alternative method of controlling liquid load up in gas wells. They propose gas circulation from the surface down the annulus, and up the tubing to achieve high velocities that will carry the liquids up to the surface. They also used Turner’s method to calculate a critical gas rate to assure removal of liquids from the wellbore.

2.1.9 H. Yamamoto, R.L. Christiansen (1999)

Yamamoto and Christiansen (1999) presented an alternative for enhancing liquid lift from low-pressure gas reservoirs. They state that the most fundamental solution for the liquid loading problem is to select tubing diameter for the well such that the natural energy in the reservoir will give a gas velocity sufficient to lift liquids from the sand face of the reservoir to the surface. Unfortunately, the optimum diameter varies for different periods in the life of the well.

By introducing restrictions, such as orifices, inside the tubing to alter flow mechanisms, liquids may be lifted by gas flow rates below the conventional accepted critical rate using Turner’s approach. Their experiments proved that the restrictions alter two-phase flow behavior and improve liquid lifting rate in their experimental flow loop. According to their results, the
20% upward adjustment of the critical flow rate equation suggested by Turner was not needed to fit their results.


Nosseir et al. (2000) presented an analysis, and an explanation of the 20% upward adjustment of the critical flow rate equation suggested by Turner, and provided a new approach for understanding the loading phenomenon concerning flow regime changes, hence converting the droplet model empirical equation into a generalized analytical approach.

They stated that the main idea behind the droplet model is very valid, however the previous discrepancies of the model with actual data were because flow regime considerations were ignored. Upon calculating the critical flow rate for a gas well, care should be given to the prevailing flow conditions so as to apply the appropriate drag coefficient for each case. There is the possibility of having more than one flow regime in a well, depending on where the calculations are being made (at the well head or at the sand face). They recommend that the calculations should be carried out using the wellhead pressure because it is the point at which the gas slippage, and hence the gas velocity, is at its maximum value. Using the maximum gas velocity will insure a maximum critical flow rate to unload the well.

2.1.11 M.Li, L. Sun, S. Li (2001)

Li et al. (2001) presented a modification to Turner’s critical gas velocity formula, considering that the liquid droplets entrained in the high velocity gas core, tend to be flat shape. By this assumption, they calculated the drag coefficient to have value of 1.0 instead of 0.44 as Turner proposed for a spherical shape droplet. The results calculated under this assumption leads to smaller values of critical gas velocities than the ones calculated with Turner’s assumption, however, the predicted results match with practical data of gas wells in China.

2.2 ZERO NET LIQUID FLOW HOLDUP

Liquid Holdup, $H_L$, is defined as the in-situ liquid volume fraction, which is the liquid volume in a pipe or annulus element ($V_L$) divided by the volume of the element ($V_P$) at in-situ conditions.

$$H_L = \frac{V_L}{V_P} = \frac{A_L}{A_P}$$

$A_L$ is the average flow area occupied by liquid, and $A_P$ is the element area. It is necessary to obtain the liquid holdup in order to determine mixture density, effective viscosity, mixture surface tension, and the actual gas and liquid velocity. Liquid Holdup can be measured by using resistivity or capacitance probes, nuclear densitometers, measuring the trapped liquid volume between two quick closing valves, visual observation through a clear tube, or dynamically by the use of differential pressure cells.

No-slip Liquid Holdup, $\lambda_L$, is defined as the in-situ liquid volume fraction if the gas and liquid phases travel at the same velocity (No slippage).
\[ \lambda_L = \frac{Q_L}{Q_L + Q_G} \]  
(2.7)

\( Q_L \) is the liquid rate at in-situ conditions and \( Q_G \) is the gas rate at in-situ conditions.

Gas Holdup or void fraction, \( H_G \), is defined by:

\[ H_G = 1 - H_L \]  
(2.8)

In two-phase flow, gas and liquid velocities can be defined in two ways. Superficial velocity is defined as the velocity that would occur if only that phase flows in the pipe. The superficial velocities of the liquid and gas phases are:

\[ v_{SL} = \frac{Q_L}{A_p} \]  
(2.9)

\[ v_{SG} = \frac{Q_G}{A_p} \]  
(2.10)

The mixture velocity is the summation of superficial velocities of both phases, and is given by:

\[ v_M = v_{SL} + v_{SG} = \frac{Q_L + Q_G}{A_p} \]  
(2.11)

Actual velocity is the flow rate at in-situ conditions divided by the actual area that the phase occupies. The actual velocities of the liquid and gas phases are:

\[ v_L = \frac{Q_L}{A_L} = \frac{Q_L}{H_L A_p} = \frac{v_{SL}}{H_L} \]  
(2.12)

\[ v_G = \frac{Q_G}{A_G} = \frac{Q_G}{(1 - H_L) A_p} = \frac{v_{SG}}{(1 - H_L)} \]  
(2.13)

The actual velocities of the two phases are typically different. The slip velocity is then defined as the difference between the actual gas and liquid velocities which is the relative velocity between both, given by:

\[ v_S = v_G - v_L = \frac{v_{SG}}{(1 - H_L)} - \frac{v_{SL}}{H_L} \]  
(2.14)

The phenomenon of accumulation of liquid in the well until a constant fraction of the well is occupied by liquid with gas flowing through it and no liquid is being carried over to the surface is known as Zero Net Liquid Flow (ZNLF) holdup. To establish these stable flowing
conditions in a well, different flow patterns are expected to be present and to change along the length of the well depending on the superficial gas velocity at the point of interest.

As liquid accumulation begins, an annular flow pattern will be present first, and as accumulation continues, transitions, beginning at the bottom of the well, to churn, slug and bubble flow will occur as holdup increases towards the ZNLF condition. The stable flow pattern established at the bottom of the well will ultimately depend on the steady-state superficial gas velocity at that depth.

Under vertical Zero Net Liquid Flow (ZNLF) conditions, the superficial liquid velocity is zero. The gas slip velocity at this condition is defined as $v_{G0}$ and ZNLF liquid holdup as $H_{L0}$, and the gas slip velocity at ZNLF is defined by:

$$v_{G0} = \frac{v_{SG}}{(1 - H_{L0})}$$

(2.15)

Thus, ZNLF holdup is defined by:

$$H_{L0} = 1 - \frac{v_{SG}}{v_{G0}}$$

(2.16)

Areas in the petroleum industry where this behavior is important, and has been applied, include predicting bottom hole pressure (BHP) in pumping oil wells (Podio et al., 1980 and Hasan 1985, 1988), and more recently, the design of compact separators such as the Gas-Liquid Cylindrical Cyclone (Arpandi et al., 1995).


Podio, Tarrillion and Roberts (1980) were the first to present a correlation to evaluate an equivalent gradient to calculate bottom hole pressure in wells under rod pumping. They did not mention the concept of zero net liquid flow holdup, but in their experimental evaluation, the conditions of ZNLF holdup were achieved to evaluate a gradient correction factor (GCF). They developed an “S” shape curve to express liquid gradient correction factor as a function of the superficial gas velocity. Their experimental data matched very well with their developed model.

2.2.2 Y. Taitel, D. Barnea (1983)

Taitel and Barnea (1983) presented a model for predicting flow pattern and pressure drop for counter current vertical gas-liquid flows. The phenomenon of counter current vertical gas-liquid flow has been studied primarily in connection with flooding and flow reversal. The flooding phenomenon is associated with the limit of downward countercurrent liquid flow, under gravity caused by gas flowing upwards driven by pressure difference. Prediction techniques for the flooding process rely heavily on experimental correlations.

Taitel and Barnea (1983) selected the correlation developed by Wallis (1969), which relates the superficial velocities of the liquid and gas at the flooding point as the most applicable for their study. The flow pattern associated with this process is counter current annular flow.
The flow patterns that can be observed in vertical counter current flow are bubble flow, slug flow and annular flow. However unlike co-current flow (either upward or downward) where the flow pattern is a fairly unique function of the flow rates, multiple solutions for the flow patterns can occur in counter current flow. Further more, counter current flow may not exist for certain values of liquid and gas flow rates as observed in Figure 2.3.

Annular flow seems to be the most natural flow pattern of counter current flow according to Taitel et al. It was the only flow pattern obtained with an open exit at the lower end at their experimental device as shown in Figure 2.2. The liquid was in the form of a falling film for a wide range of gas flow rates up to the flooding point at which the down coming liquid was swept upward. The flooding phenomenon is the limiting possible solution for counter current flow. The flooding line is the transition boundary from annular flow to no solution. According to Taitel et al., there is no acceptable theory for an accurate prediction of the flooding process. For the purpose of their study, they used the Wallis correlation given by Equation (2.17):

\[
\left[ \frac{v_{SG} \rho_g^{\frac{1}{2}}}{\sqrt{g d_i (\rho_l - \rho_g)}} \right]^2 + \left[ \frac{v_{SL} \rho_l^{\frac{1}{2}}}{\sqrt{g d_i (\rho_l - \rho_g)}} \right]^2 = C
\]

(2.17)

In this equation \(v_{SG}\) and \(v_{SL}\) are the superficial velocities of the gas and liquid respectively, \(\rho_g\) and \(\rho_l\) the gas and liquid densities, \(g\) the acceleration of gravity and \(d_i\) the pipe diameter. \(C\) is an empirical constant of the order of unity. The transition line with \(C=1\) is shown in Figure 2.3 for air-water in a 5 cm diameter pipe by transition “a”.

Figure 2.2 Flow patterns in vertical counter-current two phase flow (Taitel et al., 1983)
2.2.3 D.A. Papadimitriou, O. Shoham (1991)

Papadimitriou and Shoham (1991) presented a mechanistic model for predicting annulus bottom hole pressure in pumping wells. Their mechanistic model is based on the concept of zero net liquid flow. Their model was developed for bubble flow and slug flow under vertical ZNLF, and conducted a detailed sensitivity analysis for a typical pumping well. The effect of parameters such as casing pressure, gas flow rate, and the annulus degree of eccentricity were studied to see their influence on bottom hole pressure.

Their calculation procedure starts with the surface casing pressure, and the pressure drops in the gas region and the two-phase flow region are determined utilizing a standard iterating algorithm to integrate the pressure gradient down the annulus. Both the gas and the two-phase regions are divided into calculation increments. For each increment the pressure drop is determined by a trial and error procedure. In the gas region the pressure gradient is determined based on a single-phase gas flow, while on the two-phase region, their procedure is based on ZNLF condition.

2.2.4 S. Amaravadi, K. Minami, O. Shoham (1994)

The effect of pressure on two-phase ZNLF in upward inclined pipes was studied experimentally and theoretically by Amaravadi et al. (1994). Their experimental data were acquired for inclination angles from the horizontal of 1°, 2°, 5° and 9°, for system pressures of 14.7, 44.7 and 64.7 psia. They found that when increasing pressure, the average liquid holdup increased too, and the critical gas velocity decreased. Also increase in the inclination angle resulted in increased values of holdup and critical gas velocities.
They developed a model for the prediction of the critical gas velocity based on the existence of a solution for the combined momentum equation for equilibrium in stratified flow for ZNLF conditions. This model was developed for nearly horizontal conditions in which the liquid film model in stratified flow is the main transport mechanism.


Arpandi et al. (1995) presented experimental data and an improved mechanistic model for the Gas- Liquid Cylindrical Cyclone (GLCC) separator. Their model enables the prediction of the hydrodynamic flow behavior in the GLCC, including the operational envelope, equilibrium liquid level, vortex shape, velocity, holdup distributions and pressure drop across the GLCC.

As seen in Figure 2.4, the two-phase zero net liquid flow phenomenon occurs in the upper part of the GLCC, above the inlet. Although two-phase flow is observed under ZNLF conditions, only gas is produced off the top of the GLCC, while the liquid phase remains in the upper part of the GLCC. The liquid volume fraction in the upper part of the GLCC is referred to as the zero net liquid holdup, $H_{L0}$.

![Figure 2.4 Gas-Liquid Cylindrical Cyclone Separator Schematic](image)

For ZNLF, assuming churn / slug flow in the upper section of the GLCC, the gas velocity was developed from a modified Taylor bubble rise velocity expression given by Equation (2.18):

$$v_{GO} = C_0 v_{SG} + 0.35 \sqrt{gd_i \left( \frac{\rho_i - \rho_s}{\rho_i} \right)}$$

(2.18)

A constant value for the coefficient $C_0$ was assumed for slug / churn flow equal to 1.15 based on their experimental data. The liquid holdup for the ZNLF conditions is given by Equation (2.19):
\[ H_{L0} = \left[ 1 - \frac{v_{SG}}{v_{G0}} \right] \left( 1 - \frac{L_d}{L_p} \right) \]

(2.19)

The second term in this equation, is a correction to account for the liquid holdup that will exist only in that portion of pipe that is in slug-churn flow. \(L_p\) stands for the length of pipe, and \(L_d\) represent the length of pipe in annular flow, or the length of the droplet region. This portion of the pipe will not be included in the calculation of liquid holdup, since very little liquid exists in the annular flow region. The equation for the length of the droplet region was derived from a droplet ballistic analysis using \(K_d = 0.44\) as suggested by Turner et al. (1969) as shown in Equation (2.20) below,

\[ L_d = \frac{1}{2g} \left( \frac{K_d}{\rho_g} v_{SG}^2 \right) \left( \frac{3}{32 \rho_l \sigma g_v} \right) \]

(2.20)

Note that this equation can be rearranged to determine the critical velocity \(v_{SCrit}\) given by \(v_{SG}\) when the length of pipe in annular flow, or the length of the droplet region equals the length of the pipe \((L_d = L_p)\).

### 2.2.6 R.W. Duncan (1998)

Duncan (1998) performed an experimental study to investigate the effect of pressure in ZNLF holdup. His experimental data showed that as pressure increases, ZNLF holdup decreases as can be seen in Figure 2.5. He found that Arpandi’s model performed well at low pressures around 100 psig, but significant disagreement was observed as pressure increased. He proposed a new model to account for the effect of pressure through the concept of normalized velocity using the critical gas velocity developed by Turner et al., (1969). This method allows the use of Arpandi’s model, which accounts for different geometries and fluids, while extending the results to high pressure using the normalized velocity concept.

![Figure 2.5 Variation of ZNLF holdup with pressure (Duncan, 1998).](image)
The following steps can summarize his method:

Calculate $v_{G0}$ using Eq. (2.18) for a range of $v_{SG}$

- Calculate $H_{L0}$ using Eq. (2.19) for the same range of $v_{SG}$ and corresponding $v_{G0}$ (Calculation of $L_d$ with Equation (2.20) will be required at this point).

- Plot $v_{SG}$ against $H_{L0}$

- Calculate $v_{Scrit}$ at the same pressure as the data points.

- A straight line is constructed from the critical velocity calculated ($v_{Scrit}$) back to where it intersects the early part of the curve as a tangent. This is equivalent to using the Arpandi’s model to calculate $H_{L0}$ until the intersection of the tangent line is found. Duncan (1988) showed that this intersection corresponds to a limiting value of superficial gas velocity and a limiting value of holdup (See Figure 2.6).

- To calculate the liquid holdup for superficial gas velocities greater than this one, the straight-line Eq. (2.21) can be used until $v_{Scrit}$

$$H_{L0} = \left( H_{L0} \right)_{Limit} \frac{v_{Scrit} - v_{SG}}{v_{Scrit} - (v_{SG})_{Limit}} \quad (2.21)$$

- New values of $v_{SG}$ are read from the graph and divided by the critical gas velocity ($v_{Scrit}$) to determine a normalized velocity ($v_{SG} / v_{Scrit}$).

- This is then plotted against $H_{L0}$ to form the normalized velocity curve.

- This curve can then be used to evaluate $H_{L0}$ at any pressure condition, once $v_{Scrit}$ is evaluated at those conditions.

![Figure 2.6 Normalized curve construction.](image-url)

Chirinos et al. (2000) performed an experimental and theoretical study in the GLCC compact separator on liquid carry-over phenomenon. Based on the difference between the ZNLF holdup data reported by Duncan (1998), and the model, developed by Arpandi, a pressure correction factor, $F_p$ was developed to predict the ZNLF holdup at high pressure conditions.

The expressions developed for the pressure correction factor are given by equations (2.22) and (2.23):

$$F_p = 3.7176 \ P^{-0.2633} \quad (\text{For } P>146.5 \ \text{psig})$$

$$F_p = 1 \quad (\text{For } P \leq 146.5 \ \text{psig})$$

The prediction of the ZNLF holdup for high pressure is given by Equation (2.24):

$$H_{L0} = \left[ 1 - \left( \frac{v_{SG}}{v_{G0}} \right) \right] \left[ 1 - \frac{L_d}{L_p} \right] \ F_p \ \left( 1 + \frac{\phi}{100} \right)$$

Where $\phi$ is the percent liquid carry-over. This equation is valid for $H_{L0}>0.2$. Therefore for $0<H_{L0}<0.2$ the ZNLF holdup is based on the interpolation between 0.2 and 0 using the corresponding superficial gas velocities as follow:

$$H_{L0} = \frac{0.2(v_{SG} - v_{Scrit})}{(v_{SG[0.2]} - v_{Scrit})} \quad (\text{For } 0<H_{L0}<0.2)$$

Their experimental data showed a good agreement with their model.


An experimental study was conducted by An et al. (2000) to investigate the effects of liquid density and viscosity on ZNLF holdup in vertical pipes. They found that ZNLF liquid holdup increases as liquid density increases for the same superficial gas velocity. There appears to be a logarithmic relationship between liquid density and ZNLF holdup. As liquid viscosity increases from 1 to 100 cp the ZNLF holdup is increased significantly. An increase in yield point increases the ZNLF holdup. With high yield points, fluid remains adhered to the inside of the pipe, stabilizing holdup at very high superficial gas velocities.

2.3 **OFF-BOTTOM WELL CONTROL**

Despite success from improved technology and training, blowouts and underground blowouts still happen. Frequently these events occur during times when the drill pipe is partially removed from the well, preventing use of conventional blowout prevention procedures. At other times, these events damage the drill pipe, preventing control fluid from being placed at the bottom of the well. These situations require the use of “off-bottom” well control procedures.
Rigorous engineering design and analysis methods for off-bottom kill procedures have never been developed. For the specific case of an off-bottom dynamic kill, the tendency for the dense kill fluid to fall through, and flow counter-current to, the formation fluid flowing up the well can assist in killing the well. Currently, the effect of counter-current flow of kill fluid below the depth of the injection string is not typically considered, which results in a potentially feasible method of well control being overlooked. The research that has been performed on this effect is described herein.

2.3.1 E.M. Blount and E. Soeiinah (1981).

The dynamic kill technique is a relatively new one that was first introduced, explained and documented by Blount and Soeiinah as a response to the blowout in the well No. C-II-2 of the Arun field in Indonesia in 1978. This approach had been used much earlier in the early 1960’s in the Arkoma Basin in “relief wells” (Grace, 1994), but was not documented and investigated as a well control technique until Blount did so.

“Dynamic kill is an interim condition where a blowout is killed by injecting a fluid through a communication link and up the blowout annulus at such rate that the static formation pressure is exceeded and the well ceases to produce. The flow is multiphase (produced fluid plus injected fluid) before the well is killed and single phase (injected fluid only) immediately after the well is killed (Blount et al. 1981).”

This technique takes advantage of the pressure drop caused by the friction and hydrostatic loads generated by the injected fluid and formation fluid flowing in a multiphase flow up to the surface. The sum of the frictional pressure, and the hydrostatic pressure generated by the multiphase flow, must be greater than the static formation pressure, until a heavier static fluid can replace the lighter dynamic fluid. The injection rate can be varied to control the bottom-hole pressure by adjusting the frictional component and the average density in the annulus much in the same way the backpressure is controlled with an adjustable choke when conventionally circulating out a kick on a drilling rig. The principle that this method uses is based on the same one that is used for production analysis for producing wells, also known as NODAL™ analysis.

In the original conception of this technique, Blount et al. pointed out a particular approach of controlling the well initially using a fluid lighter that the one required to statically control the formation. In practice, this characteristic has not been fulfilled consistently, and still satisfactory results have been obtained. If a lighter fluid is used, due to the dynamic characteristics, the well is under control only while this lighter fluid is being circulated at the designated kill rate. If for any reason the circulation is stopped or suspended, the frictional pressure component of the system will be lost, and the well will go back to the uncontrolled flowing condition.

This technique was originally designed to control blowout wells by establishing communication between a relief well, and the blow out well. This technique has also been applied to control blowout wells with shallow gas flows, using a diverter (Koederitz et al. 1987), underground blowouts (Wessel et al. 1991, and Smith et al. 1997) and surface blowouts (Osornio et al. 2001).

The first study considering the countercurrent flow of killing fluid falling through formation fluid was conducted and published by John D. Gillespie, Richard F. Morgan and Thomas K. Perkins in 1990. They considered the application of the dynamic kill principle to an off bottom situation. Figure 2.7 shows the scenario of a workover well-control situation in which sand is being washed from a well that has a hole in the tubing. A dangerous situation can develop when the last sand bridge is washed through and the well tries to flow. Flow through the hole in the tubing at 250 ft also cut a hole in the casing, and there was concern that if the well was shut-in, the risk of the well cratering was great. Thus for this operation, a method was needed to design contingency plans based on a dynamic kill.

Gillespie et al. provided a method for estimating the conditions at which liquid would begin to fall through the gas, but provided no means for estimating the liquid holdup that would exist at a given injection rate. They pointed out that the mechanism of breakup of the liquid into droplets is a complex process controlled by aerodynamic and hydrodynamic effects. They also mentioned that a conservative estimate of those conditions leading to a well kill could be obtained by determining two factors: the largest diameter of droplet likely to exist in the gas stream and a conservative value for the drag coefficient of the droplet.

They presented three different methods to estimate the maximum likely droplet size. The first one is based on work developed by Hinze (Hinze, 1949 and 1955) and Hanson (Hanson et al. 1963), and reviewed by Harper (Harper et al. 1972) given by:

\[ d_{\text{max}} = \frac{20\sigma}{\rho_c V_i^2} \]  

(2.26)
The second one was developed by Karabelas (Karabelas, 1978), who considered a liquid-liquid dispersion system given by:

\[ d_{\text{max}} \approx d_{g5} = \frac{4d_i}{\left( d_i \rho_c \frac{v^2}{\sigma} \right)^{0.6}} \]  \hspace{1cm} (2.27)

The third method they considered was the one developed by Sleicher C.A. (Sleicher, 1962) who also considered a mixture system of two liquids. His expression to estimate the maximum droplet diameter is given by:

\[ d_{\text{max}} = 38 \frac{\sigma}{\rho_c v^2} \left[ 1 + 0.7 \left( \frac{\mu_d v}{g_c \sigma} \right)^{0.7} \right] \]  \hspace{1cm} (2.28)

The gas velocity at which liquid would just begin to fall is estimated as equal to the relative settling velocity of the largest droplet, just equal to the average gas velocity. This critical velocity \( v_{\text{crit}} \) is given by (Gillespie et al., 1990):

\[ v_{\text{crit}} = \sqrt{\frac{4gd_{\text{max}} (\rho_d - \rho_c)}{3\rho_c K_d}} \]  \hspace{1cm} (2.29)

The drag coefficient, \( K_d \) is a function of the Reynolds number, \( N_{Re} \), based on the slip velocity (velocity of the droplet relative to the moving gas stream):

\[ N_{Re} = \frac{d_{\text{max}} \rho_c v_s}{\mu_c} \]  \hspace{1cm} (2.30)

Gillespie et al. proposed that the evaluation of the drag coefficient should be the largest likely value, because a smaller value would lead to larger settling velocities and thus be more likely to kill the well. Equation (2.31) gives a conservative large estimate of the drag coefficient in quiescent fluid that neglects the sudden drop in value of drag coefficient associated with reaching the critical Reynolds number. The drag coefficient is calculated by:

\[ K_d = F \left[ \frac{24}{N_{Re}} + \frac{4}{N_{Re}^{0.468}} + 0.5 \right] \]  \hspace{1cm} (2.31)

They propose to use the value of \( F = 4 \) in Equation (2.31) based on the work done by Lopez and Dukler (Lopez et al. 1987), and the influence of the gas dynamics on the drag coefficient studied by Torbin and Gauvin (Torbin et al. 1961) and Clamen and Gauvin (Clamen et al. 1969). This value of \( F \) may be needed to account for some gas-turbulence intensity levels.

As mentioned earlier, they provided a method for estimating the conditions at which liquid would begin to fall through the gas, but provided no means for estimating the liquid holdup that would exist at a given injection rate. They estimated the critical gas flow rate for the specific case they were analyzing, using the three different approaches in droplet size given by
Equations (2.26), (2.27) and (2.28). Table 2.1 shows the results of these calculations of critical flow rates. The critical velocities calculated by these methods, span a very wide range of values with the highest being almost 4 times higher than the lowest for a given set of conditions. Therefore, as mentioned by Bourgoyne et al. (1994) “It appears that the calculation of critical rate for removal of all of the liquid from a well during well control operations is another area where additional research is needed”.

<table>
<thead>
<tr>
<th>Depth of Kill String (ft)</th>
<th>Gas Flow Rate (MMscf/D)</th>
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<tbody>
<tr>
<td></td>
<td>Using ( d_{\text{max}} ) from Eq.2.28</td>
</tr>
<tr>
<td>2,024</td>
<td>1.4</td>
</tr>
<tr>
<td>4,045</td>
<td>1.4</td>
</tr>
<tr>
<td>6,048</td>
<td>1.4</td>
</tr>
<tr>
<td>8,050</td>
<td>1.4</td>
</tr>
<tr>
<td>10,053</td>
<td>1.4</td>
</tr>
</tbody>
</table>

2.3.3 **G.E. Kouba, G.R. MacDougall and B.W. Schumacher (1993).**

Kouba et al. (1993) presented a method for quantifying the volume fraction of kill fluid below the point of injection in an off bottom dynamic kill situation. He stated, “Accurate prediction of liquid holdup would require countercurrent flooding models beyond the scope of this work”. However he proposed expressions to calculate formation fluid rates below which a minimum value of liquid holdup can be established.

He based his work on the flow-pattern map (Figure 2.8) based on Taitel, Barnea and Dukler’s mechanistic model (Taitel et al. 1980). The transition boundary between annular and nonannular flow was developed from a force balance on a droplet of liquid in a gas stream. The minimum velocity required to suspend a liquid droplet given by Equation (2.32) marks this transition:

\[
v_{\text{crit}} = 1.593 \left[ \frac{\sigma (\rho_i - \rho_g)}{\rho_g^2} \right]^{\frac{1}{4}}
\]  

(2.32)
Equation (2.32) is the one proposed originally by Turner et al. (1969), and has been proved to provide good results by Coleman et al. (1991). They satisfactorily tested this expression in a study that determined when low-pressure gas wells would begin liquid loading. Kouba rewrote Equation (2.32) in terms of standard volumetric flow rate given by the following equation:

\[
q_{\text{gic}} = 4.87 \frac{A_p P}{z_p (T + 460)} \left[ \frac{\sigma (\rho_l - \rho_g)}{\rho_g^2} \right]^{1/4}
\]  (2.33)

Below this flow rate, injection liquid will begin to fall downward, flooding the region below the point of injection. He established that according to Barnea’s study (1987), the minimum liquid holdup present during slug flow is about 0.25. Therefore he set the lower limit liquid holdup at this value to be considered in the calculations for slug flow, and a liquid holdup of 0 for the annular flow. Based on the same flow pattern map, he presented two more expressions to calculate the minimum gas velocity and flow rate that will give the transition between slug and bubbly flow, assigning a liquid holdup of 0.75 for the bubbly flow.

Assumption of these liquid holdups is very conservative, but provide a safe approximation. In his paper, he pointed out that “this solution should be conservative (i.e., it should over predict the kill rate). The assumptions of no-slip flow above the point of injection, negligible friction below the point of injection, and minimum liquid holdup for each flow pattern all serve to under predict total pressure drop and thus over predict the kill rate.”

2.3.4 A.T. Bourgoyne, Jr., Y. Wang, and D. A. Bourgoyne (1994).

Bourgoyne et al. conducted an experimental study to provide data on liquid holdup for gas-mud mixtures in vertical and inclined annuli for conditions of low gas velocity. The experiments were performed at the PERTTL at LSU. Two topics of interest were investigated. The first, is the knowledge of the mud remaining in the well during well unloading, which is
important for the calculation of shut-in kick tolerance, and the second is the knowledge of the mud volume falling to bottom below the kill string during an off bottom dynamic kill.

They performed their experiments for two different scenarios, one on bottom, and the other off bottom. They used water and a bentonite clay drilling fluid having a density of 8.65 ppg, with a plastic viscosity of 10 cp and a yield point of 6-lb/100 ft². Deviation angles of 0°, 10°, 20°, 40°, 60° and 80° were considered.

Some authors like Johnson et al. (1991), Nakagawa et al. (1992), Mendes (1992), and Johnson et al. (1993) have displayed the results of gas slip velocity using the Zuber-Finlay (Zuber et al., 1965) plots in which gas velocity varies linearly with the average mixture velocity. Even though these experiments were performed under very low gas and mixture velocities, their findings are very important and significative. Figure 2.9 shows one of the comparative results for two different runs at 0° (vertical position), one on bottom and the other off bottom.

The liquid holdup measured beneath the bottom of the tubing for the off bottom condition, was compared to the liquid holdup measured in the eccentric annuli at a zero mud circulation rate. Only minor differences were found between liquid holdup values measured at the same gas superficial velocity, especially for inclinations of 20° or less. This indicates that the differences between pipe flow and eccentric annular flow for the size model used (6.065” ID of outer pipe, and 2.375” OD of inner pipe) were not large. Differences were expected from previous experiences reported by Bourgoyne for equivalent hole sizes above about 5 in.

A very important observation that can be made from Figure 2.9, and that is significant for this study, is that it makes little difference if the mud is introduced at the top of the column or the bottom of the column. For either situation, the liquid holdup trends toward about the same equilibrium value. Later when the experiments performed for this project are discussed, this observation will validate the use of the configuration in the experiments for the off bottom situation.

![Figure 2.9 Zuber and Finlay Plot comparing gas rise velocity at zero liquid velocity for off bottom and on bottom pipe configuration in vertical position (Bourgoyne, 1994)](image-url)
The effect of deviation angle on holdup is also important for these low velocity experiments. Figure 2.10 shows the variation of the liquid holdup with the change in inclination angle for the same superficial gas velocity. It can be seen that for a given superficial gas velocity, holdup trends to be a maximum at a deviation angle from vertical of about 50°. Also the effect of the deviation angle from the vertical, trends to increase with an increase of gas rate.

Figure 2.10 Observed effect of vertical deviation angle on liquid holdup.  
(Bourgoyne, 1994)
CHAPTER 3

NEW PROPOSED METHOD

Description of the new proposed method is divided in four sections. First, the problem is defined, and second, calculations of the critical gas velocity required to completely remove a liquid droplet in a high velocity gas core is presented. The third section presents a method for calculating ZNLF holdup based on the procedure originally proposed by Duncan and Scott (1998), and finally, the fourth section presents a general procedure to apply all these concepts to an off-bottom dynamic kill with two application examples.

3.1 DEFINITION OF THE PROBLEM

The increasing demand for energy worldwide requires more and more wells to be drilled, as hydrocarbons constitute the main source of energy nowadays. Drilling new wells, and producing oil and gas in general, always entails some risk due to the geologic uncertainty and the possibility of human error or equipment failure. “The greatest risk undertaken while drilling and producing an oil or gas well is the potential for a blowout” (Abel, 1994).

The “Dynamic Kill” technique has been used to perform off bottom kill operations, under the assumption that no liquid is falling in counter-current of the formation fluid that is flowing upwards. If this conservative assumption indicates that the kill is possible to achieve, the operator can proceed with the field operations with more confidence. However in some cases, calculations under this assumption will indicate that the kill is not possible, discarding a valuable potential solution to the problem. Counter-current flow of kill fluid falling through formation fluid that is flowing upward is being studied and evaluated in this project for a better understanding and development of an off bottom dynamic kill procedure that accounts for liquid fallback.

3.2 EVALUATION OF THE CRITICAL GAS VELOCITY

As mentioned previously, the critical gas velocity is defined as the velocity at which liquid droplets would begin to fall into a well in countercurrent to a high velocity gas core. This critical velocity is a function of the gas and liquid properties, as well as the flow characteristics of the continuous phase.

To evaluate this critical velocity based on the terminal velocity and the liquid droplet theory, the original expression, Eq. (2.5) presented by Turner et al. (1969) is adapted to the units used herein also considering the deviation angle from the vertical ($\alpha$), as shown in Eq. (3.1):

$$v_{\text{Scrit}} = 14.27 \left[ \frac{\sigma (\rho_l - \rho_g)}{K_d \cos \alpha \rho_g^2} \right]^{1/4}$$  \hspace{1cm} (3.1)

Where the gas and liquid densities are at flowing conditions. Appendix A shows how this equation can be derived based in two different concepts; the terminal velocity and liquid droplet theory as suggested by Turner et al. (1969), and the countercurrent flow of liquid and gas at the flooding point theory developed by Taitel et al. (1983).
3.2.1 **Drag Coefficient \((K_d)\) Evaluation**

The drag coefficient \(K_d\) that corresponds to the Reynolds Number at the flowing conditions of the continuous phase should be used in Eq. (3.1) as suggested by Nosseir et al. (1997). The diameter used to calculate the Reynolds number in Eq. (3.2), is the equivalent circular diameter, equal to four times the hydraulic radius for annular flow geometry, and equal to the pipe internal diameter in tubing flow geometry.

\[
N_{Re} = \frac{d_i \rho_c V_c}{\mu_c} \quad (3.2)
\]

This criteria was selected because the flow regime around the droplet is expected to be turbulent or highly turbulent due to the high gas velocities regardless of the droplet size. An iterative process is required because the critical velocity is needed to calculate the Reynolds number. It is recommended that \(K_d = 0.44\) be used for the first iteration to calculate the first estimate of critical velocity, and then a new \(K_d\) read from Figure 3.1 using the new Reynolds number calculated. Normally at blowout conditions, the Reynolds number reached is in the highly turbulent region, resulting in a drag coefficient of 0.2 rather than 0.44 as assumed by Turner et al. (1969).

![Drag Coefficient](image)

**Figure 3.1** Drag Coefficient for spheres and cylinders (From Whitaker, 1968).

3.2.2 **Surface Tension Evaluation**

Surface tension is an important parameter in Equation 3.1, and there is not much published work or research done evaluating this parameter for gas or air with non-Newtonian fluids, in particular drilling fluids. For the purpose of this work, our first approach was to use the values recommended by other authors in gas-water systems, specifically \(\sigma = 60\) dynes/cm (Beggs, 1984). Using this value when calculating the critical gas velocity, gave good agreement with the experimental results for water, and also with the 10.5 and 12 ppg mud as described in chapter 4.
Nevertheless, we decided to further investigate the effect of solids in a 10.5 and 12 ppg mud on surface tension.

To accomplish this task, a Dunuoy Ring Apparatus from the Reservoir Lab at LSU was used with samples of water, 10.5 and 12 ppg muds. This apparatus uses a platinum-Iridium wire ring, and measures the force needed to pull the ring out from the surface of the liquid. The experimental results are presented in table 3.1 and Figure 3.2 shows the set up of the actual experiment.

Table 3-1 Surface Tension Experimental Data.

<table>
<thead>
<tr>
<th>Fluid System</th>
<th>Surface Tension Measured (dyne/cm)</th>
<th>Surface Tension Measured (lbf/ft)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Water</td>
<td>67</td>
<td>0.00459</td>
</tr>
<tr>
<td>10.5 ppg Mud</td>
<td>78</td>
<td>0.00534</td>
</tr>
<tr>
<td>12.0 ppg Mud</td>
<td>78</td>
<td>0.00534</td>
</tr>
</tbody>
</table>

Figure 3.2 Dunuoy Ring Apparatus to measure surface tension of sample fluids.

The test was performed at laboratory conditions of 72°F and atmospheric pressure. The results show that the assumption of 60 dynes/cm is acceptable, causing an error in critical velocity of less than 10%. However, the following functions of surface tension versus temperature are proposed, based on the performance of air-water systems reported by Streeter (1971), and the experimental data obtained herein, where $\sigma$ is in (lbf/ft). Figure 3.3 shows a plot of these functions.
\[
\sigma = -6 \times 10^{-9}T^2 - 5 \times 10^{-6}T + 0.004911 \quad (\text{For water}) (3.3)
\]
\[
\sigma = -6 \times 10^{-9}T^2 - 5 \times 10^{-6}T + 0.005733 \quad (\text{For 10.5 and 12 ppg mud}) (3.4)
\]

---

3.3 EVALUATION OF ZNLF HOLDUP

Once \(v_{S_{crit}}\) is calculated, the concept of ZNLF holdup is proposed as a basis for calculating the liquid holdup under these conditions, using Duncan’s method (1998). The following steps show the procedure:

- **Calculate \(v_{G0}\)** using Eq. (3.5) based on Eq. (2.18), for a range of \(v_{SG}\)

  \[
  v_{G0} = 1.15v_{SG} + 0.35 \sqrt{\frac{gd_i (\rho_i - \rho_g)}{\rho_l}} \quad (3.5)
  \]

- **Calculate \(H_{L0}\)** using Eq. (3.6) (same as Eq. (2.16)) for the same range of \(v_{SG}\) and corresponding \(v_{G0}\)

  \[
  H_{L0} = 1 - \frac{v_{SG}}{v_{G0}} \quad (3.6)
  \]

- **Plot \(v_{SG}\) against \(H_{L0}\)**

- A straight line is constructed from the critical velocity calculated (\(v_{S_{crit}}\)) back to where it intersects the early part of the curve as a tangent. This is equivalent to using the Arpandi’s model to calculate \(H_{L0}\) until the intersection of the tangent line is found. Duncan et al. (1998) show that this intersection corresponds to a limiting value of superficial gas velocity and a limiting value of holdup (See Figure 3.4).
To calculate the liquid holdup for superficial gas velocities greater than the limiting value, the straight-line Eq. (3.7) can be used until $v_{Scrit}$ is reached

$$H_{L0} = \left( H_{L0} \right)_{Limit} \left( \frac{v_{Scrit} - v_{SG}}{v_{Scrit} - (v_{SG})_{Limit}} \right)$$  \hspace{1cm} (3.7)

- New values of $v_{SG}$ are read from the graph and divided by the critical gas velocity ($v_{Scrit}$) to determine a normalized velocity ($v_{SG} / v_{Scrit}$).
- This is then plotted against $H_{L0}$ to form the normalized velocity curve (Figure 3.5).

This curve can then be used to evaluate $H_{L0}$ at any pressure condition, once $v_{Scrit}$ is evaluated at those conditions.

![Figure 3.4 Limiting values of $v_{SG}$ and $H_{L0}$](image)

Figure 3.4 Limiting values of $v_{SG}$ and $H_{L0}$
3.4 APPLICATION TO OFF-BOTTOM DYNAMIC KILL

In this section, the general principles of the dynamic kill technique are applied to off bottom conditions, followed by the step by step procedure proposed herein to be applied in an off bottom dynamic kill. Finally, two case histories are analyzed under the proposed method, the first one reported by Osornio et al. (2001) of a blowout well in Mexico (Cantarell 69I), and the second one reported by Gillespie et al. (1990) in a workover well.

3.4.1 Off-Bottom Dynamic Kill Principle.

Applying a steady state system performance analysis to a blow out well in an off bottom condition, Figure 3.6 can be constructed. Considering an injection rate of kill fluid of QL3 BPM, this kill rate will not be enough to establish a dynamic kill condition, given by the continuous line, as its outflow curve, intercepts the IPR curve, this means that there are steady state conditions of combined formation and kill fluid flows that can be achieved, such that the formation is not overbalanced and the blow out continues.

It is possible that at the new steady state conditions, the velocity of the formation fluid would not be enough to prevent some of the injected kill fluid falling back into the well countercurrent to the gas. This kill fluid would then generate an additional hydrostatic head that would be acting against the formation pressure, increasing the flowing bottom hole pressure, and therefore, reduce the formation flow rate. This condition will generate the dotted lines in Figure 3.7, with a higher bottom hole pressure, which in this case will be enough to achieve a dynamic kill.

The critical factor that would trigger this process to take place is that the formation fluid velocity is low enough to allow the kill fluid droplets to fall in countercurrent flow through the formation fluid flow.

Basically the procedure proposed here, focuses on how to generate these dotted lines, which indicate a higher bottom hole pressure, and a better prediction for the dynamic kill attempt.
Figure 3.6 Dynamic kill system performance off-bottom.

Figure 3.7 Dynamic kill system performance off-bottom, considering fallback.
3.4.2 Off-Bottom Dynamic Kill Procedure.

The following step-by-step procedure is based on a steady state system performance analysis applied to a blow out well in an off bottom scenario.

1. Plot the Inflow Performance Relationship (IPR) of the reservoir where the blowout well is located (Flowing bottom hole pressure as a function of gas flow rate). This can be achieved by using any of the available models for gas reservoirs in the literature (Beggs, 1984).

2. Construct the Outflow Performance Curve, considering the blow out conditions, and wellbore geometry on the same plot with the IPR, by:
   a. Assume a gas flow rate (This could be 1/10 of the absolute open-flow potential (AOFP) of the well.)
   b. Starting with wellhead pressure equal to atmospheric pressure in the case of sub-sonic flow, or the appropriate well head pressure in case of critical (sonic) flow, calculate flowing bottom hole pressure considering single phase gas flow (Cullender and Smith’s method (Beggs, 1984) can be used for this purpose).
   c. Increase gas flow rate and repeat step 2 (b) as needed to generate the outflow curve.

3. Find the intersection between the Inflow and Outflow curves. This point will define the initial blow out conditions of flow rate \( q_{gi} \) and flowing bottom hole pressure \( P_{wf_i} \).

4. Starting at \( P_{wf_i} \), and with \( q_{gi} \), calculate the pressure and superficial gas velocity \( v_{SG} \) at the depth where the end of the drill pipe is, which corresponds to the injection point, considering single-phase gas flow.

5. Calculate \( v_{Scrit} \) at this point, using Eq. (3.1) and add it to the plot:
   - If \( v_{SG} \geq v_{Scrit} \) No fallback occurs.
   - If \( v_{SG} < v_{Scrit} \) Liquid holdup is greater than zero:
     a. Drilling fluid will remain in the well as holdup.
     b. In the case of dry well, fallback will increase as soon as the kill process begins, increasing bottom hole pressure.

6. Construct Outflow Performance Curves, for different injection kill rates on the same plot with the IPR, by:
   a. Assume an initial injection kill rate \( q_{L1} \) (Consider a pump rate in the range of the pumping capacity available on location).
b. Assume a gas flow rate (Could be 1/10 of the rate at blow out conditions for first iteration).

c. Starting with wellhead pressure equal to atmospheric pressure in the case of sub-sonic flow, or the appropriate well head pressure in case of critical (sonic) flow, calculate the flowing pressure and superficial gas velocity \( v_{SG} \) at the injection depth, considering multi-phase flow (A multiphase flow correlation such as Hagedorn and Brown (Brill et al. 1999), or mechanistic model such as Ansari et al. (Brill et al. 1999) can be used for this purpose).

d. Compare \( v_{SG} \) at this point to \( v_{Scrit} \) from step 5:

If \( v_{SG} \geq v_{Scrit} \) ⇒ Calculate flowing bottom hole pressure \( (P_{wf}) \), considering single-phase gas flow up from the bottom of the well (Cullender and Smith’s method (Beggs, 1984) can be used for this purpose).

If \( v_{SG} < v_{Scrit} \) ⇒ Fallback will occur. Calculate ZNLF holdup using the normalized curve method proposed in section 3.3, and calculate flowing bottom hole pressure \( (P_{wf}) \), considering the fallback effect (An’s et al. (2000) method can be used for this purpose).

e. With the same injection kill rate, increase gas flow rate and repeat steps 6(c) and 6 (d) as needed to generate the outflow curve.

f. Find the intersection between the Inflow and Outflow curves. This point will define the flowing equilibrium point (pressure and flow rate) for this assumed initial injection kill rate \( q_{L1} \).

g. Increase the injection kill rate to \( q_{L2} \) and repeat the procedure from steps 6 (b) to 6 (f) to find the intersection between the Inflow and Outflow curves. Keep increasing the injection kill rate to \( q_{Ln} \) and repeat the procedure from steps 6 (b) to 6 (g) up to a point in which the Inflow and Outflow curves do not intercept each other. This condition will define the kill flow rate required for the blowout well.

3.4.3 Cantarell 69I Analysis (Case History).

In this section, an analysis of a blowout well in Mexico (Cantarell 69I) in an off bottom condition is presented. This case history reported by Osornio et al. (2001) describes a blow out which occurred while completing a nitrogen injection well at the gas cap of the Cantarell field offshore Mexico.

The gas flow rate at blow out conditions through the 9 5/8 in. injection string was estimated as 230 MMscfd with a flowing bottom hole pressure of 808 psi, and a static formation pressure of 839 psi. This Paleocene Brecia formation has a very high productivity index, making the analysis more complex, as a small change in bottom hole pressure would result in a
significant change in gas flow rate. Figure 3.8 shows the well geometry and conditions during the blow out.

Figure 3.8 Well geometry and conditions for Cantarell 69-I well (Osornio et al., 2001)

Applying the procedure described herein and with the data provided by Osornio et al. (2001), Figure 3.9 was constructed. It shows the system performance analysis for this blow out well at the original blow out conditions, and the performance under five different injection fluid rates of 12 bpm, 16 bpm, 18 bpm, 20 bpm, and 24 bpm.
For this case, the calculated pressure at the injection depth of 2230 ft for the blow out conditions was 602 psi with a calculated critical velocity of 8.58 ft/sec, corresponding a critical gas flow rate of 14.24 MMscfd. The superficial gas velocity at blow out conditions at this point was 141 ft/sec. As could be seen from this graph, gas flow rates and therefore superficial gas velocities for all the calculated outflow curves, at the different injection kill rates evaluated, are much higher than the critical gas flow rate and velocity for this case. As a result, for all these points, single-phase gas flow was considered when estimating the flowing bottom hole pressure.

According to this analysis, an injection kill rate higher than 18 bpm would have achieved control before the superficial gas velocity reached critical gas velocity. Once this injection kill rate was achieved, flowing bottom hole pressure would increase to a point at which formation gas flow stopped, consequently, kill fluid could begin falling through the static gas column, initiating a flooding process of the off bottom section of the well, that finally controlled it just by its hydrostatic column, as it was enough to statically control the well. Osornio et al. states that the well was controlled with a kill rate between 16 bpm and 20 bpm, twenty minutes after these kill rates were reached.

In this particular case, the dynamic kill did not rely on the fallback effect, due to the high gas flow rates, and low pressure. However the proposed analysis method provides an important tool to evaluate the kill attempts, showing that the dynamic kill would rely only on the friction pressure losses and hydrostatic effect in the injection string, and not on the fallback effect.
3.4.4 Potential Off-bottom Dynamic Kill in Workover Well (Gillespie’s Case)

In this section, Gillespie’s et al. (1990) case where he analyzed a potential dynamic kill of a workover well in off bottom conditions is analyzed using the new proposed method. Section 2.3.2 in chapter two, shows the background of this case in which the countercurrent flow of kill fluid falling through formation fluid is considered during an off bottom dynamic kill. For this analysis we considered the critical case where the injection string is located at 2024 ft. inside the 2 7/8 in. tubing, which has a total length of 10,791 ft. as shown in Figure 3.10.

Based on the data provided (Gillespie et al., 1990), and following the proposed method in here, a critical gas velocity of 5.1 ft/sec was calculated, resulting in a critical flow rate of 2.9 MMscf/D. The steady state system performance analysis Figure 3.11 was constructed showing the inflow performance of the reservoir and the outflow curves for four different kill rates considered, which appear as continuous lines. Following the proposed step by step procedure, once the superficial gas velocity at the point of injection is less than the critical gas velocity, the effect of fall back is considered when calculating the bottom hole pressure, which will be higher than the case if single-phase gas flow is assumed. This performance is plotted as dotted lines for each kill rate in Figure 3.11.
According to this analysis, an injection kill rate of 4 BPM would generate a bottom hole pressure capable of overcoming formation pressure, leading to a well control situation. Ignoring the fall back effect results in a bottom hole pressure lower than formation pressure, which indicates a flowing condition from the reservoir, therefore the well will not be under control. In this particular case, increasing the flow rate above 4 BPM would generate surface injection pressures high enough to exceed the 10,000 psi limit in the surface equipment. In this case then, it is important to know that relying on the fall back effect, and knowing the conditions under it will occur, will guarantee the success of the kill attempt, considering the surface equipment limits and capabilities.

Figure 3.12 shows the normalized curve constructed for this particular case, that helped to calculate the bottom hole pressures considering the fall back effect. Figure 3.13 shows the same information displayed in Figure 3.11, but in a log-log plot as presented by Gillespie to better appreciate the effect of the liquid fallback.

These examples demonstrate potential applications but do not validate the method, therefore full scale experiments were performed in both an inclined flow tube, chapter 4, and an actual well, chapter 5, to validate specific aspects of this method including $v_{scrit}$ and $H_{L0}$.
Figure 3.12 Normalized curve for the work over well.

Figure 3.13 System performance analysis for workover well (log-log plot).
CHAPTER 4

EXPERIMENTS IN 48FT FLOW LOOP

In this chapter, I will present the experimental procedure, results, and analysis for the tests made in the 48 ft flow loop at the Petroleum Engineering Research and Technology Transfer Laboratory (PERTTL) of LSU. This study measured the liquid fallback during simulated blowout conditions. The purpose of these experiments was to validate the method proposed in chapter 3, and establish a basis for developing a procedure for controlling blowouts that relies on the accumulation of liquid kill fluid injected while the well continues to flow. The results from experiments performed with air as the blowout fluid and water, 10.5 ppg and 12.0 ppg mud as the kill fluid in an experimental 48 ft flow loop at 0°, 20°, 40°, 60° and 75° deviation angles from the vertical are presented. Results of this study are also presented by Flores-Avila et al. (2002b). The results show that the critical velocity that prevents control fluid accumulation can be predicted by adapting Turner’s model of terminal velocity based on the liquid droplet theory to also consider the flow regime of the continuous phase when evaluating the drag coefficient, as well as the angle of deviation from the vertical. Similarly, the amount of liquid that flows countercurrent into and accumulates in the well can be predicted based on the concept of zero net liquid flow (ZNLF) holdup.

4.1 EXPERIMENTAL PROGRAM

The experimental phase for the 48ft flow loop in this study was performed at the (PERTTL) of LSU. The experimental program for this phase consisted of:

- Design of the Experiments
- Evaluation, modification and reconditioning of the 48 ft flow loop, and the whole experimental system.
- Calibration of all transducers in the system, and preliminary tests for adjustments.
- Definition of the data collection procedure.
- Test with air-water system at 0°, 20°, 40°, 60° and 75° deviation from the vertical.
- Test with air-mud (10.5 ppg) system at 0°, 20°, 40°, 60° and 75° deviation from the vertical.
- Test with air-mud (12.0 ppg) system at 0°, 20°, 40°, 60° and 75° deviation from the vertical.

4.1.1 Experimental Apparatus.

The experimental test apparatus was specially built to conduct multiphase flow research for gas kick behavior during well control operations in vertical and slanted wells, and was used
by Nakagawa (1990), Mendes (1992) and Wang (1993) for their two-phase flow studies. The original configuration was changed to handle the conditions of high superficial gas velocity required for these experiments. The design consists basically of four components:

1. Flow loop.
2. Liquid handling system.
3. Air handling system.
4. Data acquisition system.

Figure 4.1 shows a complete setup of the whole system. The flow loop shown in Figure 4.2, comprises two 48 ft pipes connected in a “U” shape. One side of the “U” contains the instrumented test sections and an inner short pipe fixed in a fully eccentric configuration at the top to simulate the off-bottom condition. The test loop can be inclined at any angle between 0° and 90° from the vertical position. The inside diameter of the outer pipe is 6.065 in, corresponding to a 6 in nominal, schedule 40, STD, 18.97 lb/ft pipe, MAWP of 1206 psig (-20/400°F). The outside diameter of the inner pipe is 2.375 in. Five transducers are fixed to the flow loop to measure pressure, temperature and differential pressure at 3 locations. Two differential pressure cells provided liquid holdup for the off-bottom section, while the third differential pressure cell gave liquid holdup measurement in the annulus opposite the drillpipe. For the purpose of this study, only the readings of the differential pressure cells in the section below the inner pipe representing the off-bottom condition were considered.
After a physical inspection to evaluate the current conditions of the flow loop, and a preliminary hydraulic test, a hole causing a leak in the lower section of the 6.065 in pipe, caused by corrosion, was located. Also the inspection showed that there might be some more spots in the pipe where the wall thickness would be not strong enough to work safely with the 1,206 psig MAWP. After repairing the leak, the flow loop was tested satisfactorily to 200 psig hydrostatically. For safety reasons, it was decided that the tests should be conducted using air instead of natural gas, and that the working pressures should not exceed 200 psig.

The liquid handling system for the experimental test loop, shown in Figure 4.1, consists of a centrifugal pump capable of handling up to 360 gpm at 55 psig, and a 20 bl tank, connected with 6 in and 3.5 in flexible hoses. The system can circulate/inject water and mud into the flow loop.

The air handling system, also shown in Figure 4.1, is composed of three air compressors, a 330 bl pressure tank (800 psig working pressure), and a Daniel senior orifice meter for measuring the airflow rate. The three compressors were capable of compressing air from atmospheric pressure up to 120 psig, to be stored in the pressure tank. This system could deliver up to 36,000 scf/hr of air for a test period of 15 minutes when the critical velocity was being evaluated.

The data acquisition system utilized a Lab View 5.1 software from National Instruments, located on a PC in the control room of the PERTTL, which collects all the data by receiving the
analog signals from the transducers in the flow loop and the Daniel airflow meter. The variables recorded in the software were:

- Differential Pressure in DP cell 1 (in of H$_2$O).
- Differential Pressure in DP cell 2 (in of H$_2$O).
- Differential Pressure in DP cell 3 (in of H$_2$O).
- Average pressure in the flow loop (psig).
- Injected airflow rate (scf/hr).
- Injected liquid rate (gpm).

Temperature at the flow loop was recorded and kept as a constant value during the test, as the variations in temperature were negligible.

4.1.2 Experimental Procedure.

During the first stage of the project, an analysis was performed to find out the range of conditions that could be reproduced in the experimental flow loop that would be representative of the actual field conditions of an off bottom blowout, considering the pressure and volume restrictions of our system. Experimental procedures developed in previous research were reviewed to find out what would be the best experimental procedure that would fulfill our requirements for high superficial gas velocities in the minimum amount of testing time, as our air source was limited for each test.

4.1.2.1 Calibration and Preliminary Tests

All the transducers that were used to perform the experiments were calibrated for the range of operation expected during the experimental runs. Table 4.1 shows the calibrated range of each of the components of the system.

Table 4-1 Operating range of the transducers used in the experiment.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Operating Range</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>DP Cell #1</td>
<td>0-150</td>
<td>Inches of H$_2$O</td>
</tr>
<tr>
<td>DP Cell #2</td>
<td>0-350</td>
<td>Inches of H$_2$O</td>
</tr>
<tr>
<td>DP Cell #3</td>
<td>0-150</td>
<td>Inches of H$_2$O</td>
</tr>
<tr>
<td>Pressure</td>
<td>0-500</td>
<td>Psi</td>
</tr>
<tr>
<td>Air flow rate</td>
<td>0-40,000</td>
<td>Scf/hr</td>
</tr>
<tr>
<td>Liquid flow rate</td>
<td>0-500</td>
<td>Gpm</td>
</tr>
</tbody>
</table>

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The tubing lines connected to the DP cells, measuring the loop differential pressure, were filled completely with water and purged after each test to ensure that fresh water was filling the tubing, as the final pressure drop calculation is based on the assumption that these lines are filled completely with fresh water. The tubing used to make these connections was 3/8 in with the exception of DP cell #2 which used a tubing of ½ in. Later on we found that when running the system with a high weight mud, the ½ in line provided better results as contamination of the fresh water with the weighted mud in the control line was minimized with this tubing size. Readings obtained with DP cell #1 for the weighted mud showed the contamination effect, and did not provide reliable readings.

### 4.1.2.2 Fluid Properties

The fluid properties of the system are shown in table 4.2. Table 4.3 shows the formulation of the 10.5 and 12.0 ppg mud. This formulation for the experiment was designed to simulate a real non-Newtonian fluid used for well control operations.

<table>
<thead>
<tr>
<th>Property</th>
<th>Water system</th>
<th>10.5 ppg system</th>
<th>12.0 ppg system</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (ppg)</td>
<td>8.33</td>
<td>10.5</td>
<td>12</td>
</tr>
<tr>
<td>Plastic viscosity (cp)</td>
<td>1</td>
<td>17</td>
<td>23</td>
</tr>
<tr>
<td>Yield point (lbf/100sq ft)</td>
<td>---</td>
<td>14</td>
<td>21</td>
</tr>
<tr>
<td>Gel Strength (lbf/100sq ft) 10 sec.</td>
<td>---</td>
<td>8</td>
<td>12</td>
</tr>
</tbody>
</table>

Table 4-3 Weighted mud formulation.

<table>
<thead>
<tr>
<th>Component</th>
<th>10.5 ppg system</th>
<th>12.0 ppg system</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bentonite</td>
<td>20 lbs/bl</td>
<td>20 lbs/bl</td>
</tr>
<tr>
<td>Baroid Aldacide G (Biocide)</td>
<td>0.5 lbs/bl</td>
<td>0.5 lbs/bl</td>
</tr>
<tr>
<td>Halliburton HEC-10 (polymer)</td>
<td>0.2 lbs/bl</td>
<td>0.2 lbs/bl</td>
</tr>
<tr>
<td>Caustic Soda</td>
<td>0.03 lbs/bl</td>
<td>0.03 lbs/bl</td>
</tr>
<tr>
<td>Barite</td>
<td>Required amount for 10.5 ppg</td>
<td>Required amount for 12.0 ppg</td>
</tr>
</tbody>
</table>

### 4.1.2.3 Test Operation

The experimental runs for each deviation angle started by positioning and locking the flow loop at the desired angle. Then the off-bottom section of the loop was loaded with water or weighted mud according to the test. Once the section was filled, and confirmed by the readings
from the pressure differential cells, air was injected at the bottom of the flow loop, at the lowest flow rate of the test matrix. Once the desired air injection rate was reached, and was flowing at a steady-state condition (which could be observed from the chart recorders), the data acquisition computer system recorded the selected parameters at a rate of 83 readings per minute for each parameter recorded. Six to seven different air injection rates were performed for each test.

In order to reach high superficial gas velocities, the exit of the flow loop was vented to the atmosphere, resulting in pressures in the flow loop in the range of 16 to 22 psig, depending on the flow conditions.

Another experimental procedure was performed to directly measure liquid accumulation due to fallback. The air injection rate was established prior to the liquid injection. Then the liquid accumulation was assumed to have stabilized at a constant value when the desired air and water were flowing at a steady-state condition, and differential pressures had stabilized. The data acquisition computer system then recorded the desired parameters. The results showed that the same holdup is obtained at the same superficial gas velocities for both test procedures; therefore for operational simplicity, the first test procedure was chosen for subsequent tests. After a test was finished, the flow loop was moved to the next deviation angle to repeat the experimental procedure. Figures 4.3 to 4.9 show the flow loop at the five different angles of deviation tested, as well as the air handling system.

Figure 4.3 Experimental Flow loop at 0° deviation
Figure 4.4 Experimental Flow loop at 20° deviation

Figure 4.5 Experimental Flow loop at 40° deviation
Figure 4.6 Experimental Flow loop at 60° deviation

Figure 4.7 Experimental Flow loop at 75° deviation
4.2 EXPERIMENTAL RESULTS

Figures 4.10 to 4.18, summarize the results of the tests performed in the experimental flow loop at the five different deviation angles considered of 0°, 20°, 40°, 60° and 75° with the three different fluids. In these figures, WA denotes the tests performed with water, M1 denotes the tests made with 10.5 ppg mud, and M2 denotes the tests made with 12.0 ppg mud, followed
by the angle at which each one was performed. The average superficial gas velocities for all angles, ranged from 0.34 m/sec to 12.61 m/sec. The ranges for the zero net liquid flow holdups for these angles were from 0.74 to 0.0 (critical gas velocity). For the test performed with water, in each test point there were two holdups obtained from the readings from the two different differential pressure cells for which values were very similar in all cases. The holdup considered representative for the results for this fluid, was the simple average of these two readings.

For the cases with the weighted mud (10.5 and 12.0 ppg), the readings from DP cell #1 showed the effect of mud contamination, meaning that the weighted mud was introduced into the tubing sensing the differential pressures in the DP cell, affecting the results and providing erroneous values. As mentioned earlier, this DP cell had a tubing size of 3/8 in and a shorter length of 8 ft 5 in while DP cell #2 had a tubing size of ½ in with a length of 27 ft. that provided a more reliable value that was confirmed by volumetric measurements after some tests. A complete table with all the data points can be find in Appendix B.

![Figure 4.10 Experimental results all angles and Fluids](image)

Figure 4.10 Experimental results all angles and Fluids
Figure 4.11 Experimental results with water in all angles.

Figure 4.12 Experimental results with 10.5 ppg mud in all angles.
Figure 4.13 Experimental results with 12.0 ppg mud in all angles.

Figure 4.14 Experimental results at 0 degrees deviation (vertical) with the three fluids.
Figure 4.15 Experimental results at 20 degrees deviation with the three fluids.

Figure 4.16 Experimental results at 40 degrees deviation with the three fluids.
Figure 4.17 Experimental results at 60 degrees deviation with the three fluids.

Figure 4.18 Experimental results at 75 degrees deviation with the three fluids.
4.3 ANALYSIS OF RESULTS

This section explains how ZNLF holdups were calculated based on the readings provided by the DP cells. Hypothetical critical velocities were also calculated for each case to be compared with the experimental results.

4.3.1 Calculation of the Gas Fraction and ZNLF holdup.

During the experiments, the liquid holdup was determined by means of the gas fraction for the two-phase flow applying the general energy equation, as suggested by Nakagawa (Nakagawa, 1990). For a given length of pipe test section, the total pressure change can be simply written as:

\[ (dp)_{Tot} = (dp)_g + (dp)_f + (dp)_a \]  

(4.1)

The term \((dp)_g\) accounts for the elevation change component, which relies on the liquid-gas mixture density. The term \((dp)_f\) accounts for the friction loss component which always causes a pressure drop in the direction of flow and is also a function of the liquid-gas mixture density and flow characteristics. The term \((dp)_a\) which accounts for the acceleration component, is the result of velocity changes during flow, and can be neglected for the constant range of velocities of interest in these experiments. The term \((dp)_{Tot}\) is the total pressure drop, which can be measured directly by the differential pressure cells.

The expressions to calculate the elevation term is then:

\[ (dp)_g = 0.052 \rho_s L \cos \alpha \]  

(4.2)

For the instrumentation set up where the high pressure line of the transducer is fully filled with water and the low pressure line is connected to the bottom of the test section, the pressure differential reading due to elevation is actually the difference between the hydrostatic pressure of the water column and the liquid-gas mixture in the pipe section:

\[ (dp)_g = 0.052(\rho_w - \rho_s) L \cos \alpha \]  

(4.3)

The term to calculate the friction component is given by:

\[ (dp)_f = -f \rho_s v_m^2 L \]  

\[ \frac{25.8}{d_i} \]  

(4.4)
Solving Equations (4.1), (4.3) and (4.4) for the liquid-gas mixture density, and knowing the density of the liquid and gas, the system can be solved to find the gas fraction and therefore the liquid holdup:

\[ \rho_s = \frac{-DP_{tot} + 0.433L \cos \alpha}{0.052L \cos \alpha + \frac{f_{v_m}^2 L}{25.8d_i}} \]  

(4.5)

Where \( DP_{tot} \) is the pressure differential measured by the transducer across the test section L. The gas fraction can be calculated as:

\[ H_{G0} = \frac{\rho_L - \rho_s}{\rho_L - \rho_G} \]  

(4.6)

And therefore the holdup is:

\[ H_{L0} = 1 - H_{G0} \]  

(4.7)

This dynamic determination of liquid holdup was compared to the static, volumetric holdup that was trapped and measured in selected tests, with a very good agreement as shown in table 4.4.

Table 4-4 Comparison of dynamic and volumetric \( H_{L0} \)

<table>
<thead>
<tr>
<th>Test</th>
<th>Water 0°</th>
<th>Water 40°</th>
<th>10.5 ppg mud 40°</th>
<th>12.0 ppg mud 20°</th>
<th>12.0 ppg mud 60°</th>
</tr>
</thead>
<tbody>
<tr>
<td>( H_{L0} ) Volumetric</td>
<td>.31</td>
<td>.44</td>
<td>.39</td>
<td>.35</td>
<td>.33</td>
</tr>
<tr>
<td>( H_{L0} ) Dynamic</td>
<td>.30</td>
<td>.43</td>
<td>.37</td>
<td>.34</td>
<td>.30</td>
</tr>
<tr>
<td>Error (%)</td>
<td>3.2</td>
<td>2.3</td>
<td>5.1</td>
<td>2.8</td>
<td>9.1</td>
</tr>
</tbody>
</table>

4.3.2 Critical velocity calculation and comparison.

Hypothetical critical velocities were calculated in an iterative process, with Gillespie’s criteria using the three equations he proposed (Equations (2.26), (2.27) and (2.28)). Then Kouba’s Eq. (2.32) was also used to calculate this critical velocity. Finally the proposed method herein was used to calculate the critical velocity using Turner’s criteria (Eq. (3.1)) with the drag coefficient based on Fig.3.1. The deviation angle, flow conditions, velocity, and gas properties...
were used in determining the Reynolds Number and drag coefficient that correspond to the flowing conditions of the continuous phase. Tables 4.5 to 4.7 show the results of these calculations for the three different fluids. Using the proposed method, the Reynolds Numbers reached during the high velocity tests were in the highly turbulent region, corresponding to a drag coefficient of approximately 0.2, different from Turner’s assumption of 0.44.

Table 4-5 Critical velocity calculation for water.

<table>
<thead>
<tr>
<th>Criteria</th>
<th>Reynolds No.</th>
<th>$\nu_{\text{crit}}$ (m/sec)</th>
<th>Extrapolated $\nu_{\text{crit}}$ (m/sec)</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gillespie 1</td>
<td>22,922</td>
<td>5.55</td>
<td>11</td>
<td>49.5</td>
</tr>
<tr>
<td>Gillespie 2</td>
<td>37,759</td>
<td>6.60</td>
<td>11</td>
<td>40.0</td>
</tr>
<tr>
<td>Gillespie 3</td>
<td>8,361</td>
<td>20.75</td>
<td>11</td>
<td>88.6</td>
</tr>
<tr>
<td>Kouba</td>
<td>-----</td>
<td>9.14</td>
<td>11</td>
<td>16.9</td>
</tr>
<tr>
<td>Proposed 0° Dev.</td>
<td>487,625</td>
<td>11.13</td>
<td>11</td>
<td>1.2</td>
</tr>
<tr>
<td>Proposed 20° Dev.</td>
<td>495,267</td>
<td>11.31</td>
<td>11.5</td>
<td>1.7</td>
</tr>
<tr>
<td>Proposed 40° Dev.</td>
<td>521,222</td>
<td>11.90</td>
<td>12.5</td>
<td>4.8</td>
</tr>
<tr>
<td>Proposed 60° Dev.</td>
<td>579,887</td>
<td>13.24</td>
<td>13</td>
<td>1.8</td>
</tr>
<tr>
<td>Proposed 75° Dev.</td>
<td>683,655</td>
<td>15.61</td>
<td>12</td>
<td>30.1</td>
</tr>
</tbody>
</table>

Table 4-6 Critical velocity calculation for 10.5 ppg mud.

<table>
<thead>
<tr>
<th>Criteria</th>
<th>Reynolds No.</th>
<th>$\nu_{\text{crit}}$ (m/sec)</th>
<th>Extrapolated $\nu_{\text{crit}}$ (m/sec)</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gillespie 1</td>
<td>29,528</td>
<td>5.97</td>
<td>12</td>
<td>50.3</td>
</tr>
<tr>
<td>Gillespie 2</td>
<td>48,751</td>
<td>7.09</td>
<td>12</td>
<td>40.9</td>
</tr>
<tr>
<td>Gillespie 3</td>
<td>29,582</td>
<td>21.53</td>
<td>12</td>
<td>79.4</td>
</tr>
<tr>
<td>Kouba</td>
<td>-----</td>
<td>9.80</td>
<td>12</td>
<td>18.3</td>
</tr>
<tr>
<td>Proposed 0° Dev.</td>
<td>630,507</td>
<td>11.94</td>
<td>12</td>
<td>0.5</td>
</tr>
<tr>
<td>Proposed 20° Dev.</td>
<td>619,569</td>
<td>12.22</td>
<td>12.5</td>
<td>2.2</td>
</tr>
<tr>
<td>Proposed 40° Dev.</td>
<td>652,037</td>
<td>12.86</td>
<td>13.5</td>
<td>4.7</td>
</tr>
<tr>
<td>Proposed 60° Dev.</td>
<td>697,801</td>
<td>14.43</td>
<td>14.5</td>
<td>0.5</td>
</tr>
<tr>
<td>Proposed 75° Dev.</td>
<td>817,432</td>
<td>17.03</td>
<td>12</td>
<td>41.9</td>
</tr>
</tbody>
</table>

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Table 4-7 Critical velocity calculation for 12.0 ppg mud.

<table>
<thead>
<tr>
<th>Criteria</th>
<th>Reynolds No.</th>
<th>$v_{scrit}$ (m/sec)</th>
<th>Extrapolated $v_{scrit}$ (m/sec)</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gillespie 1</td>
<td>25,950</td>
<td>6.28</td>
<td>12</td>
<td>47.7</td>
</tr>
<tr>
<td>Gillespie 2</td>
<td>44,096</td>
<td>7.54</td>
<td>12</td>
<td>37.2</td>
</tr>
<tr>
<td>Gillespie 3</td>
<td>29,351</td>
<td>22.63</td>
<td>12</td>
<td>88.6</td>
</tr>
<tr>
<td>Kouba</td>
<td>-----</td>
<td>10.33</td>
<td>12</td>
<td>13.9</td>
</tr>
<tr>
<td>Proposed 0° Dev.</td>
<td>599,057</td>
<td>12.58</td>
<td>12</td>
<td>4.8</td>
</tr>
<tr>
<td>Proposed 20° Dev.</td>
<td>612,328</td>
<td>12.76</td>
<td>12</td>
<td>6.3</td>
</tr>
<tr>
<td>Proposed 40° Dev.</td>
<td>632,281</td>
<td>13.49</td>
<td>14</td>
<td>3.6</td>
</tr>
<tr>
<td>Proposed 60° Dev.</td>
<td>703,446</td>
<td>15.00</td>
<td>14.8</td>
<td>1.4</td>
</tr>
<tr>
<td>Proposed 75° Dev.</td>
<td>834,577</td>
<td>17.66</td>
<td>12.2</td>
<td>44.8</td>
</tr>
</tbody>
</table>

The predicted $v_{scrit}$ for deviation angles from 0° up to 60° for the three different fluids, correspond well with a power-law extrapolation of ZNLF holdup to zero as shown in Figures 4.19 to 4.30. In these figures $v_{SG}$ is plotted against the experimental $H_{L0}$ and the theoretical critical velocities for the different criteria are also indicated. Also as shown in tables 4.5 to 4.7, the difference between the values of the extrapolated $v_{scrit}$ to the one calculated with the proposed method, is less than 7% in all these cases.

For the case of 75° deviation, and as shown on Figures 4.31 to 4.33, this power law extrapolation of ZNLF holdup to zero, does not show a good agreement. For angles greater than 60°, the theory of liquid droplets entrained in the high velocity gas core is probably no longer the mechanism that governs the process, yielding to inaccurate results. As previously observed by Bourgoyne et al. (1994), holdup at a given superficial gas velocity tends to be a maximum at a deviation angle from vertical of about 50°. In our case, the maximum holdup was observed to occur at about 60°. For this reason, it was considered that for deviation angles greater than 60°, and up to 75° which is the maximum in this study, the equivalent angle to be used in Eq. 3.1 would be given by $\alpha_{equ}=120 - \alpha_{real}$. This proposed equation is just based on the symmetry that $H_{L0}$ vs deviation angle curve shows, adjusting the results to the experimental data. Under this assumption, the new $v_{scrit}$ for 75° for water is 12.14 m/sec, 13.25 m/sec for 10.5 ppg mud, and 13.74 m/sec for the 12.0 ppg mud. These new values show a good agreement with a power-law extrapolation of ZNLF holdup to zero as shown in Figures 4.34 to 4.36 with differences of 1.6%, 10.4% and 12.6% for each case.
Figure 4.19 ZNLF holdup vs. $V_{SG}$ water 0° deviation

Figure 4.20 ZNLF holdup vs. $V_{SG}$ water 20° deviation
Figure 4.21 ZNLF holdup vs. $V_{SG}$ water $40^\circ$ deviation

Figure 4.22 ZNLF holdup vs. $V_{SG}$ water $60^\circ$ deviation
Figure 4.23 ZNLF holdup vs. $V_{SG}$ 10.5 ppg mud 0° deviation

Figure 4.24 ZNLF holdup vs. $V_{SG}$ 10.5 ppg mud 20° deviation
Figure 4.25 ZNLF holdup vs. $V_{SG}$ 10.5 ppg mud 40° deviation

Figure 4.26 ZNLF holdup vs. $V_{SG}$ 10.5 ppg mud 60° deviation
Figure 4.27 ZNLF holdup vs. $V_{SG}$ 12.0 ppg mud 0° deviation

Figure 4.28 ZNLF holdup vs. $V_{SG}$ 12.0 ppg mud 20° deviation
Figure 4.29 ZNLF holdup vs. $V_{SG}$ 12.0 ppg mud 40° deviation

Figure 4.30 ZNLF holdup vs. $V_{SG}$ 12.0 ppg mud 60° deviation
Figure 4.31 ZNFL holdup vs. $V_{SG}$ water 75° deviation

Figure 4.32 ZNFL holdup vs. $V_{SG}$ 10.5 ppg mud 75° deviation
Figure 4.33 ZNFL holdup vs. $V_{SG}$ 12.0 ppg mud 75° deviation

Figure 4.34 ZNFL holdup vs. $V_{SG}$ water 75° deviation with equivalent angle assumption
Figure 4.35 ZNLF holdup vs. $V_{SG}$ 10.5 ppg mud 75° deviation with equivalent angle assumption

Figure 4.36 ZNLF holdup vs. $V_{SG}$ 12.0 ppg mud 75° deviation with equivalent angle assumption
4.3.3 ZNLF Holdup Calculation

From Figures 4.11 to 4.18, for deviation angles of 0°, 20°, and 40° from the vertical, ZNLF holdup increases as liquid density increases, for the same superficial gas velocities in the range from 0 up to 8 m/sec. For higher velocities, this effect is minimized, tending to approximately the same value of ZNLF holdup. For deviation angles of 60° and 75° the density effect is minimum along all the range of superficial gas velocities studied, showing approximately the same ZNLF holdup regardless the density.

The ZNLF holdup versus normalized superficial gas velocity \( \left( \frac{v_{SG}}{v_{Scrit}} \right) \) curve calculated using Duncan’s method shows good agreement with the experimental data for the water case at all deviation angles for normalized superficial gas velocities greater than 0.2, and a slight under prediction for values around 0.1, as seen in Figure 4.37.

For the 10.5 ppg, and 12.0 ppg muds, the normalized superficial gas velocity \( \left( \frac{v_{SG}}{v_{Scrit}} \right) \) curve calculated using Duncan’s method shows a slight under prediction for values less than 0.2 as observed in the water case too, and a slight over prediction for values of normalized superficial gas velocity \( \left( \frac{v_{SG}}{v_{Scrit}} \right) \) between 0.4 and 0.6, having only a good match for the angles of 20° and 40° as observed in Figures 4.38 and 4.39.

Table 4.8 shows the average percentage of the difference between the normalized curve values, and the experimental data for each particular case, as well as the total average for each fluid. From this table it is observed that the water case shows a better performance with the smaller difference of 17.4%, and that the 10.5 ppg and 12.0 ppg muds show a similar performance with a difference slightly higher than 22%. The overall performance of the experimental data, and the normalized curve is observed in Figure 4.40.

<table>
<thead>
<tr>
<th>Deviation Angle (Deg.)</th>
<th>Water Difference (%)</th>
<th>10.5 ppg mud Difference (%)</th>
<th>12.0 ppg mud Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°</td>
<td>20.8</td>
<td>29.5</td>
<td>23.5</td>
</tr>
<tr>
<td>20°</td>
<td>13.3</td>
<td>12.9</td>
<td>21.8</td>
</tr>
<tr>
<td>40°</td>
<td>16.5</td>
<td>14.9</td>
<td>13.6</td>
</tr>
<tr>
<td>60°</td>
<td>18.5</td>
<td>17.9</td>
<td>19.3</td>
</tr>
<tr>
<td>75°</td>
<td>18.4</td>
<td>36.2</td>
<td>32.9</td>
</tr>
<tr>
<td>Total Average</td>
<td>17.4</td>
<td>22.7</td>
<td>22.2</td>
</tr>
</tbody>
</table>
Figure 4.37 Normalized ZNLF holdup curve for water

Figure 4.38 Normalized ZNLF holdup curve for 10.5 ppg mud
Figure 4.39 Normalized ZNLF holdup curve for 12.0 ppg mud

Figure 4.40 Normalized ZNLF holdup curve and experimental data for the three fluids and all angles
CHAPTER 5

FULL SCALE EXPERIMENTS IN RESEARCH WELL

In this chapter we will present the experimental procedure, results and analysis for the full-scale experiments performed at the LSU #1 research well located at the Petroleum Engineering Research and Technology Transfer Laboratory (PERTTL) of LSU. This study measured the liquid fallback during simulated blowout conditions. The purpose of these experiments were to validate the method proposed herein in chapter 3, and establish a basis for developing a procedure for controlling blowouts that relies on the accumulation of liquid kill fluid injected while the well continues to flow. The results from full-scale experiments performed with natural gas and water based drilling fluid in a vertical 2787-ft deep research well are presented. Results of this study are also presented by Flores-Avila et al. (2002a). The results show that the critical velocity that prevents control fluid accumulation can be predicted by adapting Turner’s model of terminal velocity based on the liquid droplet theory to also consider the flow regime of the continuous phase when evaluating the drag coefficient. Similarly, the amount of liquid that flows countercurrent into and accumulates in the well can be predicted based on the concept of zero net liquid flow (ZNLF) holdup.

5.1 EXPERIMENTAL PROGRAM

The experimental phase for the full-scale test in this study was performed in the LSU#1 research well at the PERTTL of LSU. The experimental program for this phase consisted of:

- Design of the Experiments
- Set up of the circulating system and equipment required to perform the test.
- Calibration of all transducers in the system, and preliminary tests for adjustments.
- Definition of the data collection procedure.
- Test with natural gas and 8.7 ppg mud system in the vertical well

5.1.1 Experimental System.

The experimental system consisted basically of four components:

1. Experimental well.
2. Liquid handling system
3. Gas handling system
4. Data acquisition system.
The LSU#1 experimental research well located at the front of the PERTTL consists of a 2787 ft deep 8 5/8 in, 36 lb/ft, J-55 closed end casing string with a burst rating of 4,460 psig. A 2746 ft string of 5 ½ in, 14 lb/ft, K-55 open-ended casing is located inside the 8 5/8 in casing. A tapered open ended string consisting of 1,583 ft of 2 7/8 in, J-55 tubing and 1,107 ft of 4.0 in, 11 lb/ft, J-55 integral joint tubing at the top, is located concentrically inside the 5 ½ in casing, to a depth of 2690 ft. The final string, a 1.9 in, 2.9 lb/ft, J-55 open ended integral joint tubing is inside and extends through the tapered string to a depth of 2,722 ft. Figure 5.1 shows a simple schematic of this well, and Figure 5.2 shows the actual well at the facility.
The liquid handling system upstream of the well, consists of a centrifugal charging pump, a flow meter, a diesel engine-driven Halliburton HT-400 triplex pump (Fig 5.3) configured to pump 2.82 gal/stk at a maximum set pressure of 4,500 psi, and a 250 bl tank. Downstream of the well, a choke manifold containing a 10,000 psi adjustable drilling choke, and a high capacity mud-gas separator are located. After the mud-gas separator, a flow meter was installed to measure the liquid returning back to the mud tanks.
The gas handling system is supplied from a natural gas pipeline operating at 600 psig. A Daniel senior orifice meter is used to measure the incoming gas flow rate. There are also three gas storage wells and a high-pressure gas compressor capable to compress the gas up to 3500 psig at the well facility. For the specific case of this experiment, this capability was not required, and the experiment was performed with the supply pipeline pressure. There is also a flare tower (Figure 5.4) after the mud-gas separator to burn the gas that has been used during the experiment, and an additional Daniel orifice meter to measure the gas rate out of the well. This system was configured to deliver up to 133,139 scf/hr of gas during these tests.

![Figure 5.4 Flare tower at the well facility](image)

The data acquisition system utilized a Lab View 5.1 software from National Instruments, located on a PC in the control room of the PERTTL, which collects all the data by receiving the analog signals from the transducers on the well and the Daniel gas flow meter. The variables recorded in the software were:

- Gas injection pressure (psig).
- Surface pressure at the 5 ½ x 4-2 7/8 in annulus to monitor bottom hole pressure (psig).
- Surface pressure at the 4-2 7/8 x 1.9 in annulus to monitor bottom hole pressure (psig).
• Gas injection rate (scf/hr).
• Return gas rate (scf/hr).
• Injected mud rate (gpm).
• Mud return rate (gpm).
• Injection mud pressure (psig)

Temperature at the test was recorded as a constant value, as the variations in temperature were negligible, also pit level was recorded manually during the tests.

5.1.2 Experimental Procedure,

During the first stage of the project, an analysis was performed to find out the range of conditions that could be reproduced in the experimental well that would be representative of the actual field conditions of an off bottom blowout, taking advantage of the full scale facility. Experimental procedures developed in previous research were reviewed to find out what would be the best experimental procedure that would fulfill our requirements for high superficial gas velocities.

5.1.2.1 Calibration and Preliminary Tests

All the transducers that were used to perform the experiments were calibrated for the range of operation expected during the experimental runs. Table 5.1 shows the calibration range of each of the components of the system.

Table 5-1 Operating range of the transducers used in the experiment.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Operating Range</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gas injection pressure</td>
<td>0-1000</td>
<td>psig</td>
</tr>
<tr>
<td>Monitor line 1</td>
<td>0-1000</td>
<td>psig</td>
</tr>
<tr>
<td>5 ½ x 4-2 7/8 in annulus</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Monitor line 2</td>
<td>0-1000</td>
<td>psig</td>
</tr>
<tr>
<td>4-2 7/8 x 1.9 in annulus</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Injection gas flow rate</td>
<td>0-150,000</td>
<td>Scf/hr</td>
</tr>
<tr>
<td>Return gas rate</td>
<td>0-150,000</td>
<td>Scf/hr</td>
</tr>
<tr>
<td>Injected mud rate</td>
<td>0-500</td>
<td>gpm</td>
</tr>
<tr>
<td>Return mud rate</td>
<td>0-500</td>
<td>gpm</td>
</tr>
<tr>
<td>Injection mud pressure</td>
<td>0-1000</td>
<td>psig</td>
</tr>
</tbody>
</table>
5.1.2.2 **Fluid Properties.**

The fluid properties of the mud system used are shown in table 5.2. Table 5.3 shows the formulation of the 8.7 ppg mud. This formulation for the experiment was designed originally to conduct experiments for the “Top Cement Pulsation” project, and was used after those experiments for the present project.

<table>
<thead>
<tr>
<th>Table 5-2 Fluid properties for the experiment.</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Property</strong></td>
</tr>
<tr>
<td>---------------------------------------</td>
</tr>
<tr>
<td>Density (ppg)</td>
</tr>
<tr>
<td>Plastic viscosity (cp)</td>
</tr>
<tr>
<td>Yield point (lbf/100sq ft)</td>
</tr>
<tr>
<td>Gel Strength (lbf/100sq ft) 10 sec.</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Table 5-3 Mud formulation.</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Component</strong></td>
</tr>
<tr>
<td>----------------</td>
</tr>
<tr>
<td>Bentonite</td>
</tr>
<tr>
<td>Baroid Aldacide G (Biocide)</td>
</tr>
<tr>
<td>PAC (polymer)</td>
</tr>
<tr>
<td>Caustic Soda</td>
</tr>
</tbody>
</table>

5.1.2.3 **Test Operation**

Natural gas (0.58 SG) was injected down the 1.9 in tubing, and circulated back to the surface by the 8 5/8 in x 5 ½ in annulus while monitoring bottom hole pressures through the static gas columns in the 4-2 7/8 in x 5 ½ in annulus and the 1.9 in x 4-2 7/8 in annulus. Seven tests were performed on this well, covering flow rates from 58,000 scf/hr up to 133,000 scf/hr.

Prior to the gas circulation, a known volume of 8.7 ppg drilling mud, was placed in the 8 5/8 in x 5 ½ in annulus to be partially displaced by the gas, reaching a zero net liquid flow holdup at a particular steady state gas rate. Adjusting a choke downstream of the casing outlet from the well controlled the gas rate.

The recovered liquid at the surface was measured in the mud tank. When liquid flow at the surface stopped, a zero net liquid flow condition was achieved, then the difference between the original known volume and the recovered volume was recorded as the remaining liquid.
volume in the well. The zero net liquid flow holdup was calculated as the ratio of the remaining liquid volume to total annulus volume. This is indicative of the fraction of the liquid or control fluid that will be accumulated in the well during flowing conditions. The stabilized gas flow rate was recorded and then the choke was opened until a higher stabilized rate was achieved.

To ensure that the value of the holdup reached during the test corresponded to the real ZNLF holdup, during some tests, some more liquid was pumped down the 1.9 in string to partially load the annular test section again. Gas rate was then held constant until a ZNLF condition was reached. If the amount of liquid pumped was the same as the amount of liquid recovered during the confirmation procedure, then the ZNLF holdup was the same, and the test results were verified.

5.2 EXPERIMENTAL RESULTS

Table 5.4 summarizes the results of the seven tests performed in the experimental well. Superficial gas velocities were calculated at average conditions between bottom hole and surface for each test. The average superficial gas velocities ranged from 0.59 m/sec to 4.83 m/sec. The corresponding zero net liquid flow holdups ranged from 0.45 to 0.046. Average pressures ranged from 698 psia to 209 psia.

<table>
<thead>
<tr>
<th>Qg (scf/Hr)</th>
<th>vSG (m/sec)</th>
<th>Average Pressure (psia)</th>
<th>Surface Pressure (psia)</th>
<th>Bottom Hole Pressure (psia)</th>
<th>H_L0</th>
</tr>
</thead>
<tbody>
<tr>
<td>58,063</td>
<td>0.59</td>
<td>698</td>
<td>496</td>
<td>900</td>
<td>0.450</td>
</tr>
<tr>
<td>64,714</td>
<td>1.24</td>
<td>500</td>
<td>295</td>
<td>705</td>
<td>0.260</td>
</tr>
<tr>
<td>84,694</td>
<td>1.42</td>
<td>340</td>
<td>110</td>
<td>570</td>
<td>0.240</td>
</tr>
<tr>
<td>102,810</td>
<td>3.04</td>
<td>255</td>
<td>95</td>
<td>415</td>
<td>0.110</td>
</tr>
<tr>
<td>117,254</td>
<td>3.98</td>
<td>223</td>
<td>67</td>
<td>379</td>
<td>0.087</td>
</tr>
<tr>
<td>130,530</td>
<td>4.60</td>
<td>215</td>
<td>63</td>
<td>367</td>
<td>0.057</td>
</tr>
<tr>
<td>133,139</td>
<td>4.83</td>
<td>209</td>
<td>58</td>
<td>360</td>
<td>0.046</td>
</tr>
</tbody>
</table>

5.3 ANALYSIS OF RESULTS

In this section the hypothetical critical velocities were calculated for the different approaches considered, and compared with the experimental results. A comparison of these results with the case of the flow loop experiments with water at 0° deviation is also presented.

5.3.1 Critical velocity calculation and comparison.

Hypothetical critical velocities were calculated in an iterative process, with Gillespie’s criteria using the three equations he proposed (Equations (2.28), (2.29) and (2.30)). Then Kouba’s Eq. (2.34) was also used to calculate this critical velocity. Finally the proposed method was used to calculate the critical velocity using Turner’s criteria, also considering the flowing conditions, velocity and properties of the gas, when determining the Reynolds Number and drag coefficient from Fig. 3.1 that corresponds to the flowing conditions of the continuous phase. Table 5.5 shows the results of these calculations. Using the proposed method, the Reynolds
Numbers reached during the high velocity tests were in the highly turbulent region, corresponding to a drag coefficient of approximately 0.2 as appears from Fig. 3.1, as opposed to Turner’s assumption of 0.44.

Table 5-5 Critical velocities calculations

<table>
<thead>
<tr>
<th>Criteria</th>
<th>Reynolds No.</th>
<th>$v_{crit}$ (m/sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gillespie 1</td>
<td>37,022</td>
<td>2.83</td>
</tr>
<tr>
<td>Gillespie 2</td>
<td>43,460</td>
<td>3.01</td>
</tr>
<tr>
<td>Gillespie 3</td>
<td>26,458</td>
<td>10.96</td>
</tr>
<tr>
<td>Kouba</td>
<td>--------</td>
<td>4.65</td>
</tr>
<tr>
<td>Proposed</td>
<td>317,798</td>
<td>5.66</td>
</tr>
</tbody>
</table>

The predicted $v_{crit}$ of 5.66 m/sec corresponds well with a power-law extrapolation of ZNLF holdup to zero as shown in Figures 5.5 and 5.6. This prediction is therefore more reliable for the test conditions than any of the methods proposed by Gillespie et al. or Kouba et al. It also gives a predicted value similar to Turner et al.’s recommended method using a drag coefficient of 0.44 and increasing $v_{crit}$ by 20%. However the proposed method should be more reliable because it explicitly considers Reynolds number and the effect on drag coefficient.

Figure 5.5 ZNLF holdup and $v_{crit}$
5.3.2 ZNLF Holdup Calculation

The ZNLF holdup versus normalized superficial gas velocity ($v_{SG} / v_{Scrit}$) curve calculated using Duncan’s method shows good agreement with the experimental data at normalized superficial gas velocities greater than 0.2 as seen in Figure 5.7. The ZNLF holdup predicted by Arpandi’s method for the test at a normalized superficial gas velocity of 0.10 is not good. Conclusions about Arpandi’s method at low velocity are not practical for this test, given only one data point consequently, further work at full scale is required to predict ZNLF holdup at low superficial gas velocities.
5.3.3 **Comparison of full scale results and flow loop results with water at 0° deviation.**

Liquid holdups for ZNLF condition for the 0° deviation in the flow loop experiments with water were compared to the ones measured in this experiment in a realistic annular geometry in a vertical 2787 ft research well, using natural gas and water based drilling fluids. The holdups in both cases follow the same trend in the normalized superficial gas velocity curve as seen in Figure 5.8. This result supports the use of the data collected in the experimental flow loop as the basis for a model to be applied in a real scenario of a blowout in a vertical well.

![Figure 5.8 Normalized ZNLF holdup curve for flow loop data of water at 0°, and 2787 ft experimental vertical well.](image)

Figure 5.8 Normalized ZNLF holdup curve for flow loop data of water at 0°, and 2787 ft experimental vertical well.
CHAPTER 6

SUMMARY AND CONCLUSIONS

Experimental data has been successfully collected to investigate the phenomenon of liquid fallback during simulated blowout conditions. Two different sets of tests were conducted for this purpose; one using an inclined flow loop, and another using a full-scale vertical research well. An overall analysis of the results obtained in this investigation leads to the following summary and conclusions:

6.1 SUMMARY

1. Liquid holdups at high superficial gas velocities have been measured in a realistic annular geometry in a vertical research well, using natural gas and water based drilling fluids.

2. Liquid holdups at high superficial gas velocities and low pressure have been measured in an experimental flow loop at deviation angles from 0° to 75°, using an air and water system. These holdups correspond well to the ones measured in the full-scale vertical research well with a realistic annular geometry, using natural gas and water based drilling fluids.

3. Liquid holdups at high superficial gas velocities and low pressure have been measured in an experimental flow loop at deviation angles from 0° to 75°, using air, 10.5 ppg, and 12.0 ppg mud systems.

4. New expressions to calculate surface tension for water, and the 10.5 ppg, and 12.0 ppg control fluids as a function of temperature are proposed here, based on experimental results. These expressions are given by Equations 3.3 and 3.4.

5. Full scale experiments in a real well, provide support for using the proposed method as a practical and accurate one for determining \( V_{krit} \), and \( H_{L0} \) in actual wells at blowout conditions.

6. ZNLF holdup increases as the deviation angle from the vertical increases, until a maximum of about 60°, and then it decreases gradually for the same superficial gas velocities. These results are similar to previous observations made by Bourgoyne et al. (1994).

6.2 CONCLUSIONS

1. The critical velocity for complete kill liquid removal due to a blowout can be predicted by modifying Turner’s model of terminal velocity, based on the liquid droplet theory, to include the effect of deviation angle and of the Reynolds number of the continuous phase on the drag coefficient.
2. The same form of the equation for critical gas velocity (Eq. 3.1) is obtained starting either from the concept of terminal velocity as suggested by Turner et al. (1969) or the concept of countercurrent flow of liquid and gas at the flooding point developed by Taitel et al. (1983).

3. Critical gas velocities calculated using Equation (3.1) showed a maximum error of 6.3% in deviation angles from the vertical from 0\(^\circ\) up to 60\(^\circ\), and 12.6\% for 75\(^\circ\) when compared to the experimental data. This provides an improvement over Gillespie’s and Kouba’s expressions, which show a maximum error of 88.6\% and 18.3\% respectively for the vertical case.

4. The Reynolds number reached during the tests performed in the flow loop, and in the full scale research well when evaluating the critical gas velocity, were in the highly turbulent region, corresponding to a drag coefficient of approximately 0.2 as appears from Figure 3.1, different from Turner’s assumption of 0.44. It is expected that at real blowout scenarios, this highly turbulent condition will be present as high gas velocities are always encountered.

5. Data collected from the flow loop can be considered as representative of realistic full-scale wells, based on the results obtained in the full-scale experiments performed in the research well, and the flow loop in vertical position.

6. Experimental evaluation of surface tension improved the evaluation of the critical gas velocity for the systems studied. Further work is needed in this area to evaluate surface tension for different systems used in well control at down hole conditions.

7. For angles greater than 60\(^\circ\), the theory of liquid droplets entrained in the high velocity gas core is probably no longer the mechanism that governs the process, yielding to inaccurate results. For deviation angles greater than 60\(^\circ\) and up to 75\(^\circ\), using an equivalent angle equal to \(\alpha_{equ}=120 - \alpha_{real}\) is suggested for Equation 3.1 to calculate \(v_{Scrit}\).

8. The method to calculate ZNLF holdup proposed by Duncan, based on normalized superficial gas velocities, showed a good agreement with the experimental data, and therefore is a good method for predicting it. Good agreement was obtained at normalized superficial gas velocities greater than 0.2 for the case of water at all angles investigated here, and the 10.5 ppg, and 12.0 ppg mud at 0\(^\circ\), 20\(^\circ\), and 40\(^\circ\) of deviation.

9. For the cases of 10.5 ppg and 12.0 ppg mud at 60\(^\circ\) and 75\(^\circ\), the normalized curve method to calculate ZNLF holdup proposed by Duncan, shows over predictions for normalized velocities less than 0.2, and under predictions for normalized velocities greater than 0.2.
10. During the experimental procedure, it makes no difference if the control fluid is injected after a constant gas injection rate is established, or if the gas is injected to unload a column of control fluid placed previously in the test loop. For both cases, the ZNLF holdup approaches the same equilibrium value once it reaches the same superficial gas velocity, therefore this method can be applied to evaluate unloading of control fluid during a blowout, or liquid accumulation during a kill procedure.

11. The concept of ZNLF holdup can be applied to evaluate the liquid accumulation or liquid fallback under the point of injection, during an off-bottom blowout scenario. Results from both tests, in an experimental flow loop, and in the full-scale research well, show good agreement with Duncan’s method for both unloading during a blowout and liquid accumulation during a kill. These results show an improvement over Kouba’s criteria of assuming a fixed value of liquid holdup for a particular flow pattern.

12. For deviation angles of 0°, 20°, and 40° from the vertical, ZNLF holdup increases as liquid density increases, for the same superficial gas velocities in the range from 0 up to 8 m/sec. For higher velocities, this effect is minimized, tending to approximately the same value of ZNLF holdup.

13. For deviation angles of 60° and 75° the density effect is minimum along all the range of superficial gas velocities studied, showing approximately the same ZNLF holdup regardless the density.

14. Considering the effect of fallback of kill fluid in the dynamic kill procedure when the superficial gas velocity is less than the critical gas velocity provides with a better prediction of the flowing bottom hole pressure. Ignoring this effect will predict a lower bottom hole pressure and therefore a higher formation flow rate, which potentially leads to an over prediction of the required kill rate to achieve the control. Therefore this model is an improved method for analyzing the potential kill of a blowout well.
CHAPTER 7

RECOMMENDATIONS

Based on the experimental experience and results of this study, the following recommendations are made to improve future experimental methods, further develop the proposed kill method for practical application, and extend the application of these concepts to other well control problems.

7.1 EXPERIMENTAL METHODS

1. When running the flow loop system with a high weight mud, the ½ in line used at the DP cell #2, provided better results than DP cell #1 when calculating the ZNLF holdup. Contamination of the fresh water in the control line with the weighted mud is apparently minimized for this tubing size. Readings obtained with DP cell #1, which used a 3/8 in control line, for the weighted mud measurements were unreliable due to mud entering the control line. For future research, it is recommended that ½ in control line be used at the DP cells to minimize this effect when using weighted mud.

2. Flushing the control line of the DP cells after each experiment with fresh water provided a good method to obtain representative readings of differential pressures for the ½ in control line. This practice should be continued in future research to have a reliable pressure differential reading.

7.2 FUTURE DEVELOPMENT OF PROPOSED KILL METHOD

3. It is recommended that additional full-scale experiments be performed at PERTTL to simulate liquid accumulation during a kill. This can be accomplished by using either of the LSU#1 or LSU#2 research wells. Control fluid can be pumped from one of the side outlet valves of the casing head with returns at the valve on the other side of the casing head, while gas is being injected through the injection string with flow out through the same valve as the fluid. These experiments would simulate off-bottom blowout conditions and allow demonstration and validation of the kill method proposed herein.

4. It is recommended that additional experiments be performed to evaluate surface tension for different fluid systems used in well control. The available models are limited to reservoir applications and may not be applicable to well control. Variations with gel strength, which is a time dependent rheological parameter of non-Newtonian fluids, is of special interest.

5. Further investigation of the performance of ZNLF holdup at lower superficial gas velocities, by incorporating Wang’s (1993) data is recommended. This will provide an improved model that could be applied with better confidence over the full range of possible superficial gas velocities.
6. Further investigation on the effect of deviation angle on critical gas velocity and ZNLF holdup to satisfactorily explain the performance observed here, and previously reported by Bourgoyne et al. (1994) is recommended. Modifications and improvements to Arpandi’s model of ZNLF holdup, including the effects of deviation angle and fluid viscosity are suggested to provide better results that could match the experimental data.

7.3 EXTENSION OF METHOD TO NEW APPLICATIONS

7. The concepts and equations in the proposed method should be implemented in a computer program to provide the user with a faster and more reliable analysis tool. The analyses of the cases studied herein were made using different and separate programs; with excel spreadsheets used for each particular case. Special consideration should be taken when selecting the two phase flow correlation or mechanistic model due to the non-Newtonian nature of some of the kill fluids used in well control, as most of the two phase flow models have been developed for Newtonian fluids.

8. Further work should be performed to implement a time-dependent model that could predict the pumping time requirement, and therefore the control fluid volume needed for a kill attempt with the liquid fall back concept. It is suggested that Taitel et al. (1983) should be considered as an starting point for this task, as they presented a model for flow pattern and pressure drop for counter current gas-liquid vertical flow.

9. The same kind of experiments described in recommendation number 3 should be performed to provide valuable information for the development of a “Dynamic Lubrication Method.” This method would be based on the current lubrication method, but with the difference that the well will not be required to be completely shut in, as the kill fluid could fall back to the bottom of the well to regain control while simultaneously bleeding gas at the surface. The dynamic feature of this new method could potentially overcome the disadvantage of the numerous pump-shut down periods required by, and resultant slow progress made with, the current lubrication method.
REFERENCES


APENDIX A

DERIVATION OF EQUATION 3.1

As a drop of liquid is a particle moving relative to a fluid in a gravitational field, particle mechanics may be employed to determine the minimum gas flow rate that will lift the droplet. A free falling particle in a fluid medium will reach a constant velocity defined as the “terminal velocity,” which will be the maximum velocity it will attain under the influence of gravity. This is due to the drag forces being equal the accelerating or gravitational forces.

Figure A.1 shows a simple diagram for the case of a single droplet in a gas stream that is flowing in a deviated well at $\alpha$ degrees from the vertical.

Figure A.1 Liquid droplet model
Making the assumption of a clean droplet of spherical shape and constant volume, and constructing the free body diagram from Figure A.1, considering the drag forces and gravitational forces we have Figure A.2:

![Free body diagram and balance of forces in a droplet.](image)

Gravitational force is given by the following equation:

\[
F_g = \frac{\pi}{6} d_m^3 g_c (\rho_i - \rho_g) \tag{A.1}
\]

Drag force is given by Equation A.2:

\[
F_{dr} = \frac{\pi}{8} d_m^3 \rho_g v_g^2 K_d \tag{A.2}
\]

The vertical component of the drag force is \(F_1\) given by:

\[
F_1 = F_{dr} \cos \alpha \tag{A.3}
\]

For the force balance condition between the drag force component and the gravitational force:

\[
F_g = F_1 \tag{A.4}
\]

Substituting Equations A.1, A.2, and A.3 in A.4:

\[
\frac{\pi}{6} d_m^3 g_c (\rho_i - \rho_g) = \frac{\pi}{8} d_m^2 \rho_g v_g^2 K_d \cos \alpha \tag{A.5}
\]

Solving for \(v_g\) we have:
\[
v_g = \sqrt[4]{\frac{4d_m g_c (\rho_l - \rho_g)}{3 \rho_g K_d \cos \alpha}} \quad (A.6)
\]

Making the same assumption as Turner for the critical Weber number equal to 30 to attain the maximum droplet size that may exist in the flow:

\[
N_{wc} = \frac{v_g^2 \rho_g d_m}{\sigma g_c} = 30 \quad (A.7)
\]

Solving A.7 for the droplet diameter:

\[
d_m = \frac{30 \sigma g_c}{v_g^2 \rho_g} \quad (A.8)
\]

Substituting A.8 in A.6:

\[
v_g = \sqrt[4]{\frac{4 \left( \frac{30 \sigma g_c}{v_g^2 \rho_g} \right)}{3 \rho_g K_d \cos \alpha}} \quad (A.9)
\]

Solving for \(v_g\) we have:

\[
v_g = \left[ \frac{120 \sigma g_c^2 (\rho_l - \rho_g)}{3 \rho_g^2 K_d \cos \alpha} \right]^{1/4} \quad (A.10)
\]

Substituting \(g_c\) for its value of 32.2 lbm-ft/lbf-sec^2, and knowing that this gas velocity will be the critical gas velocity, we finally have:

\[
v_{crit} = 14.27 \left[ \frac{\sigma (\rho_l - \rho_g)}{K_c \cos \alpha \rho_g^2} \right]^{1/4} \quad (A.11)
\]

Which is Equation 3.1.

Another way to arrive to this equation is starting with the concept of countercurrent flow of liquid and gas at the flooding point developed by Taitel et al. (1983). As could be observed from Equation (2.17) in chapter 2, for the particular case of ZNLF holdup, \(v_{SL} = 0\), leaving only the first term of the equation.
Comparing Equation (A.12) to the one developed by Turner et al. (1969) for the settling velocity of a spherical drop relative to the gas velocity, given by Equation (A.6), we can appreciate that both equations are the same if:

\[
C = \left( \frac{4}{3K_d} \right)^{1/4}
\]  
(A.13)

\[\alpha = 0\]  
(A.14)

\[d_m = d_i\]  
(A.15)

For Turner’s assumption of \(K_d=0.44\), the value of \(C\) is 1.319 instead of 1 assumed by Wallis and Taitel et al. Furthermore, Wallis mentioned that the value of \(C\) depends on the design of the ends of the tubes and the way in which the liquid and gas are added and extracted to the system in their experimental set up as shown in Figure 2.2. He found that for tubes with sharp-edged flanges, \(C=0.725\), whereas when end effects are minimized, \(C\) lies between 0.88 and 1. He also found that for inclined tubes, the flow rates at flooding points could be much higher.
### APENDIX B

#### 48 FT FLOW LOOP EXPERIMENTAL DATA

**Water 0°**

<table>
<thead>
<tr>
<th>$Q_g$ (scfh)</th>
<th>Avg.Press. (psia)</th>
<th>Avg.Temp. (°F)</th>
<th>DP2 Cell (in H2O)</th>
<th>$V_{sg}$ (m/s)</th>
<th>$V_{sg}/V_{scrit}$</th>
<th>$H_{L0}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1140</td>
<td>22</td>
<td>98</td>
<td>112.9</td>
<td>0.35</td>
<td>0.031</td>
<td>0.644</td>
</tr>
<tr>
<td>2225</td>
<td>20</td>
<td>98</td>
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**Water 75°**

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### 12.0 ppg mud 60°

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VITA

Fernando Sebastian Flores-Avila, son of Gilberto Flores-Ortiz and Gloria Ma. Avila de Flores, was born in México Distrito Federal, in January 20, 1964.

In 1986, he graduated in petroleum engineering at the Universidad Nacional Autonoma de México. Right after that, he joined Petroleos Mexicanos at Ciudad del Carmen Campeche, working as a reservoir engineer. In 1987 he joined Otis Engineering Corporation, a Halliburton Company and worked in México, North America and South America in different areas such as snubbing, wireline, coiled tubing, gas lift, completions and well control. In 1998 he received his master’s degree in petroleum engineering from the Universidad Nacional Autonoma de Mexico, and came to Louisiana State University in August of 1998 to pursue the doctoral degree in petroleum engineering under a Fulbright scholarship. He is currently with Petroleos Mexicanos working at Mexico City.

He is married to Adela Mayorga-Duran, and they have three children: Maria Fernanda (9 years old), Maria Adela (8 years old), and Camila (4 years Old).