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DEVELOPMENT AND TESTING OF A ROTARY DISC PULSER FOR PULSE COLUMN APPLICATION

A. L. Olson

IDAHO NATIONAL ENGINEERING LABORATORY

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Westinghouse Idaho Nuclear Company, Inc.
Idaho Falls, Idaho 83403

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ABSTRACT

Air pulse column contactors are a favored method of conducting radiochemical liquid-liquid separations processes. They have no moving parts and are easily operated from behind thick shielding.

A ruggedly constructed more reliable air pulsing device was developed and its operation mathematically characterized in this study. Additionally, a correlation for the column perforated nozzle plate discharge coefficient was developed.

Rigorous mass, momentum, and energy balances were solved using a computer software code entitled "Advanced Continuous Simulation Language". Experimental data from a 2-inch diameter glass column equipped with the prototypical air pulser showed that column pulse amplitudes could be predicted within 10% for the conditions studied and within 5% for normal column operating conditions.

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NOMENCLATURE

ACSL	: Advanced Continuous Simulation Language.
A	: Cross-sectional area, subscripted to refer to specific experimental system sections.
C	: Perforated plate orifice discharge coefficient.
CPM	: Pressurization/exhaust cycles applied to the pulse column per minute.
C_p, C_v	: Heat capacities at constant pressure and volume respectively.
d	: Perforated plate hole diameter.
D	: Diameter, subscripted to refer to specific experimental system sections.
e	: Subscript referring to equivalent length of pipe due to fittings, bends, etc.
f	: Fanning friction factor.
g_c	: Gravitational acceleration constant.
H	: Vertical distance from pulse column overflow to pulse leg entry to the column; also refers enthalpy.
ICPP	: Idaho Chemical Processing Plant at the Idaho National Engineering Laboratory.
J	: Energy conversion factor.
K	: Pressure loss coefficient, subscripted with e for expansion and c for contraction.
L	: Length, subscripted to refer to specific experimental system sections.
M	: Molecular weight.
MD	: Mass flow of air between rotating discs in the pulser.
N	: Number of perforated plates within the column.

N_{Re} , Re	: Dimensionless Reynolds Number.
N_{We} , We	: Dimensionless Weber Number.
P	: Pressure, subscripted to refer to specific experimental system sections.
PSIG	: Pressure in pounds force per square inch gauge.
R	: Universal Gas Constant.
Sch.	: Schedule, refers to nominal pipe size.
t	: Time.
T	: Temperature, subscripted to refer to specific experimental system sections.
TBP	: Tributyl Phosphate, organic extractant in the Purex process.
U	: Refers to the internal energy of the system.
v	: Fluid velocity, subscripted to refer to specific experimental system sections.
V	: Volume, subscripted to refer to specific experimental system sections.
W	: Refers to the work term in the Energy Equation.
X	: Vertical distance, subscripted to refer to specific experimental system sections.
β	: Ratio of small to large diameter for orifices, and expansions or contractions in pipes.
γ	: Ratio of heat capacities.
ΔP	: Pressure differential.
ϵ	: Relative pipe roughness.
μ	: Fluid viscosity.
ρ	: Fluid density.
σ	: Fluid surface tension.

INTRODUCTION

Fuel elements, or assemblies, discharged from nuclear reactors contain significant quantities of uranium and plutonium isotopes (heavy metals) which are still fissionable and represent a significant energy resource. The most common method utilized to reclaim the heavy metal is dissolution of the spent fuel followed by liquid-liquid solvent extraction.

The most widely used solvent extraction method in the nuclear industry is known as the Purex Process. Ten to thirty volume percent Tributyl Phosphate (TBP) in a normal paraffin hydrocarbon diluent, n-dodecane, is the preferred solvent at the Idaho Chemical Processing Plant (ICPP). The heavy metal is extracted according to the following overall reaction.

The reaction is shifted to the right by increasing the aqueous nitrate concentration. Stripping the heavy metal from the organic phase, which completes a solvent extraction cycle, is facilitated by decreasing the aqueous nitrate concentration, thereby shifting the reaction back to the left. The end result of this mechanism is the purification of the heavy metals since the TBP has a very low affinity for

the fission products and other impurities.

Solvent extraction operations in a radioactive environment² are generally conducted in three types of contactors; mixer-settlers, perforated plate columns, and centrifugal contactors. Mixer-settlers consist of a mixing chamber, where a rotating impeller promotes the contact between the phases, and a settling chamber where the two phases separate by gravity settling. Each mixer-settler ordinarily represents one stage and several are cascaded to accomplish either extraction, scrubbing, or stripping functions.

Centrifugal contactors are also single stage devices. The phases flow into the space between a rotating annular cylinder and a stationary outer cylinder where mixing of the phases occurs. Subsequently, the dispersed phases flow into the rotating cylinder where the phases are separated by centrifugal force.

Columns have been a popular contactor for several decades for conduct of radiochemical operations. They can easily be remotely operated from behind thick shielding required to prevent exposure of operating personnel to high radiation. They have no moving parts which can fail and require maintenance in a high radiation field. Also, they provide efficient aqueous/organic phase contact.

This study will address specifically the perforated plate pulse column as shown in Figure 1. The column consists of a

vertical tube housing the perforated plates, known as a cartridge. Expanded settling sections are located at each end of the column to prevent entrainment. The column may be either aqueous continuous (interface at the top) or organic continuous (interface at the bottom) depending on the mode of operation. It is prudent to keep the interface away from the product end of the column because of the buildup of interfacial material which may contaminate the exiting phase. Extraction and scrub columns are normally organic continuous and stripping columns are normally aqueous continuous.

Figure 1. Perforated Plate Pulse Column

During column operation the heavy, or aqueous, phase enters the top of the column and the light, or organic phase enters the bottom. As the phases pass countercurrently to one another the discontinuous phase is dispersed into droplets by the perforated plates, promoting high interfacial area and increased mass transfer. The driving force for the countercurrent flow is gravity. However, because of the restrictive nature of the perforated plates, the phases will not flow countercurrently without the supply of an external energy source.

This energy is delivered in the form of a pulse, hence the name "pulse column". The pulsating action may be supplied by a reciprocating pump with check valves or a bellows actuated by a cam. These alternatives are not desirable from the viewpoint of maintenance in a radioactive environment. The preferred method at the ICPP is air pressure applied to a pulse leg. The organic phase inlet also enters the pulse leg.

Two major items are affected by pulse energy; total volumetric throughput and mass transfer efficiency. The pulse action forces organic upward through the plates and then pushes the aqueous phase downward. Pulse energy supplied to the column is a function of frequency and amplitude. The regimes of pulse column operation are illustrated in Figure 2.¹⁰ The region to the left of the pulsed flow line indicates the column will be flooded due

Figure 2. Types of Pulse Column Operation

to inadequate pulse energy. As pulse energy is increased, total phase throughput increases to a maximum and then decreases. Several regimes of operation are achievable in the column. Mixer-settler operation is characterized by coalescence and redispersion of the dispersed phase during each pulse cycle. Quasi-emulsion operation occurs when little or no coalescence of the dispersed phase occurs during a pulse cycle. Unstable operation can be identified by localized phase inversions along the length of the column. Complete flooding occurs when countercurrent phase flow becomes impossible.

The most efficient mass transfer is obtained when interfacial area, formation of new interfacial area, and turbulence are maximized. Mass transfer efficiency in a column is measured in terms of a height equivalent to a theoretical stage (HETS). The smaller the magnitude of the HETS, usually stated in feet, the more efficient the operation. Maximum efficiency is normally observed in the quasi-emulsion operation region.

The frequency-amplitude product determines acceptable pulse column operation as shown in Figure 2. Both optimum mass transfer and column throughput are strongly influenced by this parameter.

The objective of this study was to develop an air pulsing device and mathematically model the system to allow accurate prediction of the amplitude within a pulse column. It is desirable to locate the air pulsing device outside of the intense radiation field, which is normally in the vicinity of pulse columns. It should be of sturdy construction to provide operational reliability and low maintenance. Above all, it must provide excellent control of air pressure for satisfactory pulse action.

The research plan was completed in two parts. An air pulsing device was developed, fabricated and tested in a pilot plant environment closely simulating actual production plant operation. The air pulser consisted of two rotating circular discs, enclosed by a cylindrical container and two

end plates, which alternately allow air to flow into and pressurize the pulse leg and then flow out of and vent the pulse leg. Air is allowed to diffuse out of the pulser piping system and into the vent system during pressurization and into the pulser piping system during venting. Test conditions were varied and measurements of air pressure and amplitude at the various test conditions were taken. Experimental data were compared with data predicted by the mathematical model.

In parallel, a separate experiment was designed to determine the orifice discharge coefficient of the perforated plates. An experimental apparatus was provided to measure pressure drop across the perforated plates. These data were used to correlate the orifice coefficient as a function of the dimensionless moduli of the fluid in the column. This correlation was factored into the mathematical model used to predict column amplitude. Pressure drop attributable to the perforated plates in the column significantly contributes to the determination of the column amplitude.

LITERATURE REVIEW

The present day pulse column is a result of a contactor patented by van Dijk¹⁵ in which perforated plates covering the cross-sectional area of the column were reciprocated vertically to disperse the phases.

J. D. Thornton¹⁴ is the earliest experimenter to publish work concerning an air pulsing device. He recognized the advantages of eliminating moving parts in radioactive liquid service and greater operational flexibility. The reported device utilized a double-acting piston/air cylinder arrangement. The trapped air between the piston and the process fluid transferred the energy from the piston to the process fluid. Flooding data were correlated as a function of the dispersed and continuous phase superficial velocities and fractional dispersed phase holdup at flooding. Very little is mentioned about prediction of column amplitude during operation.

Jealous and Johnson⁷ determined the theoretical power that must be applied to the liquid-liquid system by the pulse to achieve a specific column amplitude. Their work was directed toward a mechanical pulse generator, however, with no mention of air pulsing. More importantly, the work included the first development of a second order non-linear

differential equation describing the phenomena of static head, frictional losses, and acceleration/deceleration forces on the liquid system. This appears to have provided the basis for future theoretical activities.

During the late 1950's a program was initiated at the ICPP to develop an air pulser system for the production facility in place at that time. The program culminated in a developmental report by Weech, P'Pool, and MacQueen¹⁶ published in 1961. The air pulser mechanism was a pair of poppet valves operated by two motor-driven cams as illustrated in Figure 3. This concept performed reliably in both a test system and a trial plant installation. A mathematical representation of the system was also developed. The liquid portion of the system is described by a second order non-linear differential equation. The air pressure which was applied to the liquid in the pulse leg was not expressed in explicit terms, but approximated by another non-linear second order differential equation. These expressions were set up and solved using an analog computer. Agreement between calculated and experimental data points was reported as good and appears to be within 20%. Reported experimental column amplitudes were less than 1 inch. The plate orifice discharge coefficient was estimated based on a calculated Reynolds number through the perforated plate holes. The perforated plates were of the 23% free area sieve variety.

(3a) DETAILS OF AIR PULSER MECHANISM

(3b) DETAILS OF POPPET VALVE ASSEMBLY

Figure 3. Poppet Valve Air Pulser System

Hammond⁵ calculated column pulse amplitudes by assuming that the air pressure exerted on the liquid in the pulse was a square-wave function of time. Solution of the expressions was by analog computer. Mathematical analysis provided a simplified expression which agreed well with his analog solution when an arbitrary adjustment was made in the time constant used in the simplified expression.

Experimental data were not reported.

Horn⁶ continued the work at ICPP using the same experimental equipment. In addition to using an analog computer to solve the differential equations, he developed a linear approximation using Laplace transforms to represent the expressions. Very close agreement was obtained between the analog simulation and the experimental data, usually 5% or less, for the range of test conditions and experimental equipment. Amplitudes calculated by hand using the approximate mathematical solution also compared favorably with the analog simulation. Again, an estimated orifice discharge coefficient was used.

Weech and Knight¹⁷ have provided the most rigorous analysis of an air pulser system to date. Using very few simplifying assumptions, differential equations were derived from force and mass balances describing the air inlet and outlet systems. These were coupled with the expression for the liquid portion of the system and solved using a numerical method on a digital computer. This work was directed at an actual plant installation utilizing a rotating, cam operated, poppet valve system. Excellent agreement, within about 5%, was obtained between measured and computed values of pressure in the pulse leg above the liquid. An estimated plate orifice discharge coefficient is also used in this study. The plates are of the 23% free area nozzle variety rather than sieve plates.

Kaser⁸ revisited the work completed by the authors previously noted. His goal was to predict column pulse amplitude as a function of air reservoir pressure and pulse frequency via a simplified mathematical expression. He assumed the air pressure to be a sinusoidal function of time and obtained a simplified equation using Laplace transforms. It was concluded that the assumption of a square-wave air pressure variation (that of Hammond) agreed better with experimental column amplitude data at low frequencies (<50 cycles/minute) and the sinusoidal assumption was more reasonable for higher frequencies (>50 cycles/minute). Either assumption could be used for rough estimates. Again there was no allowance for a varying plate orifice discharge coefficient. His predictions were within 5% of experimental values at frequencies less than 65 cycles/minute and amplitudes less than 1 inch. Accuracies were between 20% and 50% outside of this range however.

The above described work is not amenable to the type of air pulser device being contemplated. These studies and models appear to be adequate for rough order-of-magnitude estimates but additional accuracy is desired to optimize column operation and to develop operational safety parameters which must be followed to satisfy nuclear criticality safety requirements. Additionally, the affect of unsteady-state velocity of the liquid through the column and the affect of plate geometry on the orifice discharge coefficient cannot be

assumed constant. This study was initiated primarily to characterize the pulse leg pressure-time relationship resulting from the utilization of a rotary disc pulser mechanism. Determination of the orifice discharge coefficient is included to provide greater accuracy to column amplitude predictions. The effect of introducing organic to the pulse leg, not addressed by previous work, is examined to determine if column amplitude is significantly affected.

THEORETICAL DEVELOPMENT

This study will apply the principles developed previously by the referenced authors to the rotary disc air pulser system with the following key exceptions. New terms must be derived to describe the leakage of air into or out of the pulser system. Separate mathematical descriptions of the air system will be derived to determine whether the air flow is best described isothermally or adiabatically. A mathematical description of the perforated plate orifice discharge coefficient will be developed to account for plate geometry and the unsteady-state velocity of the liquid through the column. The perforated plate orifice discharge coefficient will be developed first. This will be followed by development of mathematical descriptions of the liquid and air portions of the pulse column respectively.

A semi-empirical approach will be used in calculating and correlating discharge coefficients for the perforated plates. It will be assumed that the plate is composed of a number of individual orifices acting independently and in parallel. It is further assumed that the flow is evenly distributed through the cross-section of the column. Also, it is assumed that the free area of the plate includes the annular space between the plate edge and the inside diameter

of the column, but not the area occupied by the spacers between the plates. These assumptions provide a calculational basis that is consistent with the operation of the pulse column.

It has been shown that plate orifice coefficients can be correlated as a function of the following variables.⁹

$$C = F \left[\left\{ \frac{dv\rho}{\mu} \right\}, \left\{ \frac{d}{P} \right\}, \left\{ \frac{T}{d} \right\}, \left\{ \frac{A_f}{A_c} \right\} \right]$$

where d is the orifice diameter, v is the fluid velocity through the orifice, ρ is the fluid density, μ is the fluid viscosity, P is the hole pitch, T is the plate thickness, A_f is the free area of the plate, and A_c is the cross-sectional area of the column if no plate were installed in the column. These are physical properties of the conduit or column, the plate, and the fluid. This study will address only one specific plate; the nozzle plate. A typical column plate cartridge assembly is depicted in Figure 4.

For a circular cross-section containing an orifice, the generally accepted flow equation is:⁹

$$v = C \sqrt{\frac{2 g_c \Delta P}{\rho \left(1 - \left(\frac{A_2}{A_1} \right)^2 \right)}} \quad (1)$$

where v is the velocity through the orifice, A_2 is the orifice area, and A_1 is the conduit area. For a specific volume around a plate the Equation of Continuity gives:

$$A_c v_c = (\text{No. of holes}) A_d v_d = A_f v_f$$

Figure 4. Column Cartridge Plate Assembly

Subscripts c correspond to the column, d to the hole diameter, and f to the free cross-sectional area in the column. Rearrangement of Equation 1 above for a series of N plates in terms of the superficial column velocity and substitution gives:

$$\Delta P = \frac{N v_c^2 \rho}{2 g_c C^2} \left(\frac{A_c}{A_f} \right)^2 \left[1 - \left(\frac{A_f}{A_c} \right)^2 \right] \quad (2)$$

This expression will be used to correlate the orifice discharge coefficient, C.

An air pulsing operation performed on a column consists of alternately applying air pressure to the pulse leg and

then exhausting the pressure in the leg. Resistance to liquid movement must be overcome during the pulsing action. Application of a momentum balance to a typical pulse column system generates the following expression:

$$P_T = P_{st} + P_v + \Delta P_f + \Delta P_a \quad (3)$$

where P_{st} is the hydrostatic pressure developed by the liquid, P_v is the vent pressure above the liquid level in the column, ΔP_f is the pressure loss due friction of liquid movement, ΔP_a is the pressure loss due to acceleration of the liquid mass, and P_T is the air pressure applied to the pulse leg.

Pressure loss due to friction is composed of loss due to liquid flow in the pipes and loss due to liquid flow through the perforated plates. Friction losses in pipes, assuming steady uniform flow, can be expressed by the Fanning equation:¹¹

$$\Delta P_f = \frac{2 f L \rho v^2}{g_c D} \quad (4)$$

where f is the Fanning friction factor and is expressed as:³

$$f = 2 \left[\left(\frac{8}{Re} \right)^{12} + \frac{1}{(A + B)^{\frac{3}{2}}} \right]^{\frac{1}{12}} \quad (5)$$

where

$$A = \left[2.457 \ln \frac{1}{\left(\frac{7}{Re} \right) + \frac{0.27 \epsilon}{D}} \right]^{16}$$

$$B = \left(\frac{37,530}{Re} \right)^{16}$$

ϵ is the relative pipe roughness, D is the pipe diameter,

and Re is the Reynolds No.

Friction loss due to liquid flow through the perforated plates in terms of the liquid velocity in the pulse leg can be expressed as:

$$\Delta P_f = \frac{N}{2} \frac{v_L^2}{g_c} \frac{\rho}{C^2} \left(\frac{A_L}{A_f} \right)^2 \left[1 - \left(\frac{A_L}{A_c} \right)^2 \right] \quad (6)$$

If the mass of liquid within the pulse column and pulse leg is expressed as mass per unit area, pressure loss due to acceleration of this mass will be proportional to it's density and length.¹⁶ This mass resistance to change in velocity can be represented by:

$$\Delta P_a = \frac{\rho}{g_c} L \frac{dv}{dt} \quad (7)$$

A simplified schematic of a pulse column system is depicted in Figure 5. Two configurations are addressed herein: one without and one with organic liquid flow to the pulse leg. The normal configuration is with the organic inlet entering the pulse leg. The development is facilitated by assuming a datum line at the pulse leg entry to the column. H is the liquid height in the column from the pulse leg entry to the overflow and is assumed constant. X is the liquid height in the pulse leg above the pulse leg entry. X_1 is the liquid height in the organic inlet line above it's entry to the pulse leg. X_2 is the distance from the pulse leg entry at the column to the organic inlet entry at the pulse leg. As the development proceeds, the following

Figure 5. Schematic of Pulse Column System

nomenclature will be used: T or L subscripts refer to the pulse leg; The subscript m refers to the liquid mixture in the column; The subscript I refers to the organic inlet line; The subscript c refers to the column; Variables without subscripts or the subscript 3 refer to the conduit connecting the pulse leg and the pulser; The P subscript refers to the rotary disc pulser; The 1 subscript refers to the conduit connecting the air reservoir and the rotary disc pulser; The R subscript refers to the air reservoir; The 2 subscript

refers to the conduit connecting the rotary disc pulser and the vent; and the subscript v refers to the vent.

If the valve on the organic inlet line is closed, the liquid flow in the system can be mathematically described as follows. Frictional losses can be expressed in terms of the pulse leg liquid velocity as the sum of losses in the pulse leg and losses due to the plates in the column.

$$\Delta P_f = \left[\frac{N \rho_m}{2 g_c C^2} \left(\frac{A_L}{A_f} \right)^2 \left[1 - \left(\frac{A_f}{A_c} \right)^2 \right] + \dots \right. \\ \left. \frac{2 f \rho_L (X_e + X)}{g_c D_L} \right] \frac{dX}{dt} \left| \frac{dX}{dt} \right| \quad (8)$$

Where X_e is the equivalent length of fittings, bends, etc., in the pulse leg.

Pressure loss due to mass acceleration, in terms of liquid acceleration in the pulse leg, is:

$$\Delta P_a = \left[\frac{\rho_m H}{g_c} \left(\frac{A_L}{A_c} \right) + \frac{\rho_L X}{g_c} \right] \frac{d^2 X}{dt^2}$$

The left hand term in brackets represents resistance to change in flow in the column. It must be divided into several terms, however, to account for different cross-sectional areas in the column. The fluid will be accelerated to different velocities in the disengaging heads, in the main body of the column, and through the perforated plates. The geometry of the column and piping to be modelled defines this term. An overall mechanical energy balance can

now be established using the above pressure loss terms to give the second order non-linear differential equation describing the liquid portion of the pulsing action:

$$\begin{aligned}
 P_T = P_v + \rho_m H - \rho_L X + & \left[\frac{N \rho_m}{2 g_c C^2} \left(\frac{A_L}{A_f} \right)^2 \cdot \cdot \cdot \right. \\
 & \left. \left[1 - \left(\frac{A_f}{A_c} \right)^2 \right] + 2 f \rho_L \frac{(X_e + X)}{g_c D_L} \right] \frac{dX}{dt} \left| \frac{dX}{dt} \right| + \cdot \cdot \cdot \\
 & \left[\frac{\rho_m H}{g_c} \left(\frac{A_L}{A_c} \right) + \frac{\rho_L X}{g_c} \right] \frac{d^2 X}{dt^2} \quad (9)
 \end{aligned}$$

If the valve on the organic inlet is open, which is normally the case, an alternate energy balance is needed. Referring to Figure 5, a pressure balance around the organic inlet and pulse leg junction gives:

$$P_J = P_T + \rho_L X + \frac{2 f \rho_L (X + X_e)}{g_c D_L} \frac{dX}{dt} \left| \frac{dX}{dt} \right| + \frac{\rho_L X}{g_c} \frac{d^2 X}{dt^2} \quad (10)$$

$$P_J = P_v + \rho_L X_I + \frac{2 f \rho_L (X_I + X_{Ie})}{g_c D_L} \frac{dX_I}{dt} \left| \frac{dX_I}{dt} \right| + \frac{\rho_L X_I}{g_c} \frac{d^2 X_I}{dt^2} \quad (11)$$

$$\begin{aligned}
 P_J = P_v + \rho_m H - \rho_L X_2 - & \left[\frac{N \rho_m}{2 g_c C^2} \left(\frac{A_L}{A_f} \right)^2 \cdot \cdot \cdot \right. \\
 & \left. \left[1 - \left(\frac{A_f}{A_c} \right)^2 \right] + \frac{2 f \rho_L (X_{2e} + X_2)}{g_c D_L} \right] \frac{dX_2}{dt} \left| \frac{dX_2}{dt} \right| - \cdot \cdot \cdot
 \end{aligned}$$

$$\left[\frac{\rho_m H}{g_c} \left\{ \frac{A_l}{A_c} \right\} + \frac{\rho_l X_2}{g_c} \right] \frac{d^2 X_2}{dt^2} \quad (12)$$

From the Equation of Continuity, assuming flow down the pulse leg where $\frac{dX}{dt}$ is negative, $\frac{dX_2}{dt}$ is negative, and $\frac{dX_l}{dt}$ is positive,

$$\frac{dX}{dt} = \frac{dX_2}{dt} - \left\{ \frac{A_l}{A_l} \right\} \frac{dX_l}{dt} \quad (13)$$

and
$$\frac{d^2 X}{dt^2} = \frac{d^2 X_2}{dt^2} - \left\{ \frac{A_l}{A_l} \right\} \frac{d^2 X_l}{dt^2} \quad (14)$$

The above three equations for P_l can then be solved for X and X_l .

P_l is the air pressure applied to the liquid in the pulse leg and is a function of time and geometry of the system. It is considerably more complicated, due to compressibility effects, than the liquid system. Rigorous mass, momentum, and energy balances will be applied to the air piping system for the development of this term. The air pressures normally used are less than three atmospheres; therefore, it is assumed that the air is a perfect gas. There may be some question as to whether compressible flow can be assumed to be isothermal or adiabatic. Consequently, terms will be derived to test the validity of each assumption. The control volume concept will be utilized.

The system to be modelled is portrayed in Figure 6. It

Figure 6. Simplified Schematic of Pulser Air System

will be assumed that air flowing from the reservoir to the pulse leg will be positive and air flowing from the pulse leg to the vent is negative. The isothermal equations for P_T will be developed first, followed by the adiabatic equations.

A mass balance performed around the expansion/contraction section (Figure 6) connecting the pulse leg and the conduit to the pulser gives the following:

$$\frac{d}{dt}(\rho_T V_L) = \rho_T \frac{dV_L}{dt} + V_L \frac{d\rho_T}{dt} = \rho v A \quad (15)$$

Substitution of the ideal gas law, $\rho = \frac{PM}{RT}$, for ρ_T and taking the time derivative gives:

$$\frac{d\rho_T}{dt} = \frac{M}{RT} \frac{dP_T}{dt} \quad (16)$$

where: $V_L = A_L (L_L - X)$, and $\frac{dV_L}{dt} = -A_L \frac{dX}{dt}$

Substitution and rearrangement of these terms gives the following expression for pressure as a function of time in the pulse leg:

$$\frac{dP_T}{dt} = \frac{\left\{ \rho v A + \rho_T A_L \frac{dX}{dt} \right\} R T}{V_L M} \quad (17)$$

This equation is valid for both pulsation and venting because the sign of the velocity, v , provides the conversion.

A steady state momentum balance around the expansion/contraction section, generally accepted throughout the literature, initiates the development of P , the pressure at the expansion:

$$P - P_T = \frac{K \rho v^2}{2 g_c}$$

where K , the pressure loss coefficient, is represented by K_c for contraction and K_e for expansion. The time derivative of the above equation is:

$$\frac{dP}{dt} - \frac{dP_T}{dt} = \frac{K}{2 g_c} \frac{d}{dt} (\rho v^2) = \frac{K}{2 g_c} \left\{ \frac{v^2 M}{R T} \frac{dP_T}{dt} + \frac{P M}{R T} 2 v \frac{dv}{dt} \right\}$$

Rearranging terms gives an expression for pressure just upstream of the expansion/contraction:

$$\frac{dP}{dt} = \frac{\frac{K \rho v}{g_c} \frac{dv}{dt} + \frac{dP_T}{dt}}{1 + \frac{K M v |v|}{2 g_c R T}} \quad (18)$$

The sign of v is retained to account for pressurization or exhaust. When v is positive K is K_c and when v is negative K is K_e .

A momentum balance over a length of conduit can be written as:¹⁷

$$\frac{\rho}{g_c} \frac{\partial v}{\partial t} + \frac{2 f \rho v^2}{g_c D} + \frac{\partial P}{\partial L} = 0$$

All equations will be solved numerically for small increments of time, reevaluating for each increment. This allows use of a mean, or constant, density with only a slight error.

Integrating and rearranging gives:

$$\frac{\rho L}{g_c} \frac{dv}{dt} + \frac{2 f L \rho v^2}{g_c D} + \Delta P = 0$$

The first term is pressure loss due to acceleration and the second is pressure loss due to friction. ΔP is the pressure drop over the length, L , of the conduit and, using the Ideal Gas Law:

$$\rho = \frac{(P_1 + P_2) M}{2 R T}$$

Application of this momentum balance to the section of pipe connecting the pulser and the pulse leg results in the following expression for the air velocity in the pipe:

$$\frac{dv}{dt} = \frac{P_p - P - \frac{2 f L_f \rho v |v|}{g_c D}}{\left\{ \frac{\rho L}{g_c} \right\}} \quad (19)$$

where:

$$\rho = \frac{(P_p + P) M}{2 R T}$$

The sign of v is retained to denote direction of the air flow.

A mass balance around the pulser assembly gives:

$$\frac{d}{dt}(\rho_p V_p) = \frac{M}{R T} \frac{dP_p}{dt} = -\rho v A + \rho_1 v_1 A_1 - MD \quad \text{for } v = \text{positive}$$

$$\frac{M}{R T} \frac{dP_p}{dt} = -\rho v A + \rho_2 v_2 A_2 + MD \quad \text{for } v = \text{negative}$$

The expressions for pressure in the pulser, after rearrangement of the above equations, is:

$$\frac{dP_p}{dt} = \frac{\left(-\rho v A + \rho_1 v_1 A_1 - MD \right) RT}{M V_p} \quad \text{for } v = \text{positive} \quad (20)$$

$$\frac{dP_p}{dt} = \frac{\left(-\rho v A + \rho_2 v_2 A_2 - MD \right) RT}{M V_p} \quad \text{for } v = \text{negative} \quad (21)$$

MD represents air leakage out of the pulser to the vent during pressurization and air leakage into the pulser during the exhaust portion of pulse cycle. The flow occurs through the small gap (approximately 0.005 inch) between the rotating discs and the enclosure. The leakage, or MD , will be modelled via isothermal compressible flow through a nozzle represented by:¹

$$MD = A P_1 \sqrt{\frac{2 g_c \gamma M}{(\gamma - 1) R T} \left[\left(\frac{P_2}{P_1} \right)^{\frac{2}{\gamma}} - \left(\frac{P_2}{P_1} \right)^{\frac{\gamma+1}{\gamma}} \right]} \quad (22)$$

where A is the area available for, or resistance to, flow.

P_1 is the upstream pressure, P_2 is the downstream pressure, and γ is the ratio of specific heats. For the purposes of this study:

$$MD = A P_P \sqrt{\frac{2 g_c \gamma M}{(\gamma - 1) R T} \left[\left(\frac{P_v}{P_P} \right)^{\frac{2}{\gamma}} - \left(\frac{P_v}{P_P} \right)^{\frac{\gamma+1}{\gamma}} \right]} \quad (22a)$$

for pressure.

$$MD = A P_R \sqrt{\frac{2 g_c \gamma M}{(\gamma - 1) R T} \left[\left(\frac{P_P}{P_R} \right)^{\frac{2}{\gamma}} - \left(\frac{P_P}{P_R} \right)^{\frac{\gamma+1}{\gamma}} \right]} \quad (22b)$$

for exhaust.

Air velocities in the pipes connecting the reservoir and the pulser and the pulser and the vent are derived in the same manner as the velocity in the pipe connecting the pulser and the pulse leg. They are:

$$\frac{dv_1}{dt} = \frac{P_R - P_P - \frac{2 f L_1 \rho_1 v_1 |v_1|}{g_c D_1}}{\left(\frac{\rho_1 L_1}{g_c} \right)} \quad (23)$$

where:

$$\rho_1 = \frac{(P_R + P_P) M}{2 R T}$$

During the vent portion of the pulse cycle P_p in the above equations approaches P_R .

$$\frac{dv_2}{dt} = \frac{P_v - P_P - \frac{2 f L_2 \rho_2 v_2 |v_2|}{g_c D_2}}{\left(\frac{\rho_2 L_2}{g_c} \right)} \quad (24)$$

where:

$$\rho_2 = \frac{(P_P + P_v) M}{2 R T}$$

We now have expressions for P_T (Equation 17), P (Equation 18), P_p (Equations 20 and 21), v (Equation 19), v_1 (Equation 23), and v_2 (Equation 24) as functions of time. They can be coupled with the expressions for X (Equation 9), or X and X_1 (Equations 10, 11, and 12) and solved to determine column and pulse leg amplitudes.

An adiabatic representation of the air system can be generated with the addition of a thermodynamic energy equation for a control volume:¹³

$$\frac{d}{dt}(\rho V U) + \Delta \left[\left(H + \frac{v^2}{2g_c} + z \frac{g}{g_c} \right) \rho v A \right] + \frac{dW}{dt} - \frac{dQ}{dt} = 0$$

The following assumptions will be made for this study:

- 1) The flow is adiabatic, therefore $\frac{dQ}{dt} = 0$
- 2) Potential and kinetic energy contributions are negligible.
- 3) The heat capacities of air, C_p and C_v , are reasonably constant at the expected pressures and temperatures.
- 4) With the assumption of an ideal gas the internal energy, U , is $C_v T$ and the enthalpy, H , is $C_p T$.

The energy equation then reduces to:

$$\frac{d}{dt}(\rho V U) + \Delta(\rho v A H) + \frac{dW}{dt} = 0$$

A mass balance around the pulser (Figure 6) gives:

$$\frac{d}{dt}(\rho_p V_p) = -\rho v A + \rho_1 v_1 A_1 - MD \quad \text{for } v = \text{positive}$$

This is as previously derived above for an isothermal representation. When v is negative, the above derived expression also applies. MD again represents the air leakage around the pulser and is modelled as described above. The energy equation for the pulser, where the work term $\frac{dW}{dt}$ is 0,

is:
$$\frac{d}{dt}(\rho_p V_p U_p) + \Delta(\rho v A H) = 0$$

For pressurization and where v is positive this becomes:

$$C_v T_p \frac{d}{dt}(V_p \rho_p) + C_v V_p \rho_p \frac{dT_p}{dt} = -\rho v A C_p T_p + \rho_1 v_1 A_1 C_p T_R - MD C_p T_p$$

Substitution of the mass balance and rearrangement gives an expression for the pulser temperature:

$$\frac{dT_p}{dt} = \frac{\rho v A (C_v T_p - C_p T_p) + \rho_1 v_1 A_1 (C_p T_R + C_v T_p) + MD (C_v T_p - C_p T_p)}{\rho_p V_p C_v} \quad (25)$$

During the exhaust portion of the pulse cycle, where v is negative, the pulser temperature becomes:

$$\frac{dT_p}{dt} = \frac{\rho v A (C_v T_p - C_p T_T) + \rho_2 v_2 A_2 (C_p T_p + C_v T_p) + MD (C_p T_R - C_v T_p)}{\rho_p V_p C_v} \quad (26)$$

The pulser pressure can now be calculated from the Ideal Gas

Law:
$$P_p = \frac{m_p R T_p}{V_p} \quad (27)$$

where m_p is calculated from the mass balance.

A mass balance around the expansion/contraction section

(Figure 6) at the pulse leg gives:
$$\frac{d}{dt}(\rho_1 V_1) = \rho v a$$

The energy balance for the pulse leg becomes:

$$\frac{d}{dt}(\rho_T V_L U_T) = \rho v A - \frac{dW}{dt}$$

Expansion of this pulse leg energy equation gives:

$$C_v T_T \frac{d}{dt}(\rho_T V_L) + \rho_T V_L C_v \frac{dT_T}{dt} = \rho v A C_p T_P - \frac{dW}{dt}$$

Substitution of the mass balance and rearrangement gives an expression for pulse leg temperature during pressurization:

$$\frac{dT_T}{dt} = \frac{\rho v A \left\{ C_p T_P - C_v T_T \right\} + \frac{dW}{dt}}{\rho_T V_L C_v} \quad (28)$$

The work term represents either work done by the system on the surroundings or work done by the surroundings on the system. In this case the system is the mass of air contained in the pulse leg. During pressurization when the velocity of the liquid in the pulse leg is negative, work is being done by the air on the liquid and:

$$\frac{dW}{dt} = \frac{P_T A_L}{J} \frac{dX}{dt}, \quad \text{where } J \text{ is a conversion factor} \quad (29)$$

During exhaust when the pulse leg liquid velocity is positive work is being performed on the air by the liquid and:

$$\frac{dW}{dt} = (P_v + \rho_m H - \rho_L X) \frac{A_L}{J} \frac{dX}{dt} \quad (30)$$

The pulse leg temperature expression during the exhaust cycle when v is negative becomes:

$$\frac{dT_T}{dt} = \frac{\rho v A \left\{ C_p T_T - C_v T_T \right\} + \frac{dW}{dt}}{\rho_T V_L C_v} \quad (31)$$

The pulse leg pressure is now calculated from the Ideal Gas

Law:
$$P_T = \frac{m_T R T_T}{V_l} \quad (32)$$

where m_T is calculated from the mass balance for the pulse leg. Air velocities in the connecting conduits are calculated as in the isothermal case. Expressions for T_T (Equations 28 and 31), P_T (Equation 32), P_p (Equation 27), T_p (Equations 25 and 26), v (Equation 19), v_1 (Equation 23), and v_2 (Equation 24) as functions of time are now available to be coupled with those for X (Equation 9), or X and X_1 (Equations 10, 11, and 12), and solved.

Both the isothermal and adiabatic equations must obviously be solved simultaneously. Utilization of conventional methods to solve this second order non-linear system of equations would be extremely difficult, if not impossible. A more plausible method is numerical integration using one of several techniques.

The preferred technique for this work utilized a computer software code entitled "Advanced Continuous Simulation Language" (ACSL). This software is commercially available and is designed for modelling and evaluating the performance of continuous systems described by time dependent, non-linear differential equations. The version used for this work operated on an IBM PS/2 Model 80 Personal Computer with Ryan-McFarland Fortran. A Runge-Kutta fourth order integration routine internal to the ACSL software was used for solution of the equations. It works by evaluating the derivatives at four points across a calculation interval. A

weighted combination of these derivatives is then used to step across the interval.

ACSL coding is relatively simple and most of the basic elements are defined as in Fortran. Two important features are free form input and sorting of the continuous model equations. The sort element is in contrast to other programming languages where program execution depends critically on statement order. The model definition code is sorted by ACSL so that outputs are calculated before they are used. Simulation codes used in this work are reproduced in Appendix C.

EXPERIMENTAL EQUIPMENT

Two separate equipment setups were used to produce experimental data for this work. The test setup for the orifice discharge coefficient will be described first. This will be followed by a description of the air pulser system and pulse column respectively.

The experimental apparatus utilized in ascertaining the perforated plate orifice discharge coefficient is displayed in Figure 7. A set of 3 perforated plates are located internal to a 2-inch inner diameter glass pipe. This is similar to the plate installation in an actual pulse column. The 1/16-inch thick perforated plates (Figure 4) have nozzle holes 3/16-inch in diameter, 23% nominal free area, 3/8-inch pitch, and the nozzle portion extends 0.035 to 0.050 inches from the plate. Spacing between the plates is 2 inches. Two pressure taps, 8.5 inches apart, are available to sense the differential pressure generated by liquid flowing through the plates. The differential pressure is measured by two inclined graduated tubes, both open to the atmosphere, for the relatively small pressures which were expected to be experienced. Flow through the plates is provided by either pressurizing the organic supply tank with air, or utilizing the facility water supply. The flow of liquid was measured

Figure 7. Schematic of Orifice Coefficient Equipment

by two Fischer & Porter Model 10A1755A rotameters with ranges of approximately 50 - 600 and 100 - 1400 liters per hour of water respectively.

The rotary disc pulser was envisioned to be an enclosed device occupying equal to or less than the space required by the previous poppet valve system. It would equal the air

control performance provided by the poppet valves and be a relatively maintenance free item. It should provide a pulse frequency of 20-100 cycles per minute and withstand an internal air pressure of up to 40 PSIG without leakage to the environment. Its construction should be rugged enough to last a 20-year lifetime.

With these criteria in mind, two rotating circular discs enclosed by a cylindrical container and two end plates to alternately allow air to flow into and pressurize the pulse leg and then flow out of and vent the pulse leg were utilized. Provisions were made to allow leakage of air out of the pulser piping system and into the vent system during pressurization and leakage of pressurized air into the system during venting. This was to obviate the need for internal seals which would be subject to failure. The pulser components were carefully machined to allow for a 0.005 inch clearance between the rotating discs and the enclosure. This feature minimized the air leakage into or out of the pulse leg system.

Two major problems with the pulser were identified; one mechanical and one with air flow. The discs tend to deform due to the applied air pressure and adequate flow area must be provided during pressurization and venting. Details of the reasoning and calculations used in support of design and fabrication of the pulser are presented in Appendix A. A cross-section of the assembled pulser is shown in Figure 8.

Figure 8. Cross-Section of the Assembled Pulser

Two 8.75-inch diameter by 1.5-inch thick discs mounted on a variable diameter shaft and enclosed by a 10-inch Sch. 160 pipe and two 12-inch square end plates are the major components of the pulser. The port sections of the discs provide precise air flow control, allowing equal time for pressurization and venting. It is driven by a 1/4 horsepower motor coupled with a variable speed drive inverter and a 20.9:1 gear reducer. Rotational frequencies of 10-90 cycles/minute (or RPM) are easily achievable.

The air piping and pulse column equipment used to carry out the test program are shown in Figures 9 and 10 respectively. The pulse column is constructed of glass and has an active section, where the majority of the mass transfer occurs, 20 feet high. The active section has a 2-inch inner diameter and contains 112, 23%-free-area nozzle plates. The nozzles are oriented upward and the plates are spaced on 2-inch centers. The top calming section, or disengaging head, is a cylindrical glass pipe 8 inches in diameter and 5 feet high. The bottom disengaging section is of similar material and is 6 inches in diameter and is 2 feet high. The column is equipped with air purged dip tubes coupled with differential pressure transmitters to measure the column static weight, interface location, and density in the top disengaging head.

The pulse leg consisted of a 22 1/2 foot section of either a 1/2-inch or 1-inch inner diameter glass pipe. This

Figure 9. Pulser Air Piping Configuration

Figure 10. Pulse Column Configuration

facilitates measurement of pulse amplitude. The glass portion of the pulse leg is connected to 3/8-inch Sch. 40 stainless steel pipe which enters the column below the plate section on the bottom and 1 inch Sch. 40 pipe on the top. The organic inlet is a 1/2-inch (0.43 inches inner diameter) stainless steel tube teed into the 3/8-inch pipe.

The air piping consists entirely of 1-inch Sch. 40 pipe. The air reservoir, which provides air capacity during pulsing, is a 10 cubic foot horizontally mounted tank. Air pressure in the reservoir is regulated by a manually operated Cashco Model 1000 LP-3 pressure regulating valve. Its range is 0-30 PSIG. To prevent damage to the close tolerance rotating parts in the rotary disc pulser, a Norgren Type F46 filter is installed in the pipe connecting the reservoir and the pulser to remove particulate. Six electronic pressure transducers, Foxboro Model 841GM-AI-A, with a range of 0-15 or 0-30 PSIG are strategically located on the air piping system. Two Type K thermocouples were installed in the system: one 1/4-inch diameter for the air reservoir and one 1/16-inch diameter for the pulse leg.

Configurational dimensions are as specified in Figures 9 and 10. Measurements throughout the system are collected by a computerized data acquisition system.

EXPERIMENTAL PROCEDURE

Experimental data for this work were generated under two separate procedures. The first procedure described concerns data produced for perforated plate orifice discharge coefficient determination. This is followed by a description of how pulse leg amplitude data were produced for comparison to the theoretical model.

Four experiments were completed to simulate the pulse column liquid flow for orifice coefficient determination. Three replicates of each experiment were completed for a total of twelve runs. Water and 30-volume-percent Tributyl Phosphate in an n-dodecane diluent were the two fluids used during the experimentation. A combination of the two fluids and two flow directions through the plates made up the four experiments. Differential pressure measurements were recorded at different flow rates as the fluid passed forward through the nozzles. The plate orientation was then reversed and fluid was passed back through the nozzles while recording differential pressure at different flow rates. The experimental sequence is listed in Table 1. The fluid velocities through the plate free cross-sectional area ranged from approximately 0.13 to 2.6 feet/second.

Experimental data for the air pulser and column were

Table 1. Experimental Sequence for Orifice Coefficient Determination

<u>Run Number</u>	<u>Fluid</u>	<u>Flow Direction</u>
1	Organic	Through
2	Organic	Back
3	Water	Back
4	Water	Through
5	Organic	Through
6	Organic	Back
7	Water	Back
8	Water	Through
9	Organic	Through
10	Organic	Back
11	Water	Back
12	Water	Through

gathered at three sets of operating conditions: 1) The pulse column and associated piping were filled with water, 2) The column and piping were filled with 30-volume-percent Tributyl Phosphate in an n-dodecane diluent, and 3) Normal pulse column operation. There were no feed solutions added to the system during conditions (1) and (2). Condition (3) was an aqueous continuous environment with the perforated plated nozzles oriented upward. Reservoir pressure and pulse frequency were varied between 4 and 16 PSIG and 30 and 90 cycles per minute (CPM), respectively, for conditions (1) and (2). The points at which data were collected for the 1/2-inch pulse leg are listed in Table 2. The points at which data were collected for the 1-inch diameter pulse leg are listed in Table 3. Data at normal pulse column

Table 2. Data Collection Points for the
1/2-Inch Diameter Pulse Leg

Reservoir Pressure (PSIG)	Frequency (CPM)			
	30	50	70	90
4	X	X		
6	X	X	X	
8		X	X	X
10		X	X	X
12		X	X	X
14			X	X
16			X	X

Table 3. Data Collection Points for the
1-Inch Diameter Pulse Leg

Reservoir Pressure (PSIG)	Frequency (CPM)		
	40	60	80
4	X	X	
6	X	X	
8	X	X	X
10		X	X
12		X	X
14		X	X
16			X
18			X

operation were collected at four points: 40 CPM and 5 PSIG, 50 CPM and 6 PSIG, 60 CPM and 5 PSIG, and 70 CPM and 6 PSIG. Data which were recorded at each point included the pulse leg amplitude and average pulse leg height for the organic inlet valve closed and open, the organic inlet line amplitude and

average height when the organic inlet valve was open and the average pulse leg and reservoir air temperatures. Data from the transducers which sensed air pressure was gathered for one pressure at each pulse frequency. It was believed that the shape of the pressure versus time curve would not change radically at different pressures. Pulse leg and organic inlet line amplitudes were measured with a measuring tape after it was visually observed that these parameters were oscillating stably about a particular level.

RESULTS & DISCUSSION

Results of the perforated plate orifice discharge coefficient testing will be discussed initially. This will be followed by a discussion of the tests concerning the column and air pulser.

A typical plot of the pressure drop data collected during orifice discharge coefficient testing is shown in Figure 11. All data collected are listed in Appendix B. The mean of the three pressure drop values collected at each flow rate was used for calculation of the orifice discharge coefficient, C. C was calculated from the following (Equation 2 rearranged and in terms of the free area velocity):

$$C = \sqrt{\frac{N \rho v_f^2}{2 g_c \Delta P} \left[1 - \left(\frac{A_f}{A_c} \right)^2 \right]}$$

As can be seen in Figure 11, a slight variation in the pressure drop trend occurs at a free cross-sectional velocity of approximately 0.7 feet/second. A change in rotameter use occurs at this point and it is not believed to be an actual transition in flow regime. This change is typical of all data collected.

Plots of orifice coefficient versus Reynolds No., for forward and reverse flow through the nozzles, is shown in Figures 12 and 13 respectively. Examination of these plots

Figure 11. Typical Plot of Pressure Drop vs.
Free Area Fluid Velocity

Figure 12. Plot of Orifice Coefficient vs. Reynolds No. for Forward Fluid Flow Through Nozzles

Figure 13. Plot of Orifice Coefficient vs. Reynolds No. for Reverse Fluid Flow Through Nozzles

clearly reveals a discrepancy between the aqueous and organic fluids not compensated for by Reynolds No. The two trends need to be represented as one for a proper correlation. The discrepancy is believed to be caused by fluid physical properties since geometry is fixed. The obvious properties are density, viscosity, and surface tension. It was believed that both trends could be represented by the use of a single dimensionless property correction, or the addition of another dimensionless group, along with Reynolds No. This phenomenon was not observed by Kolodzie and van Winkle.⁹ Their work dealt with gases rather than liquids but the implication was that their data seemed to correlate well with Reynolds No.

Further examination of Figures 12 and 13 indicates that for forward flow through the nozzles the discharge coefficient initially increases rapidly with increasing Reynolds No. and then tends to become constant. It also appears to be asymptotically approaching some value over the experimental Reynolds No. range. The discharge coefficient for reverse flow through the nozzles initially behaves similarly to forward flow through the nozzles. However, it appears to approach a constant value for both fluids at a Reynolds No. of approximately 1000 and then continues through the range of experimental Reynolds No.'s.

The orifice coefficient data were plotted against Reynolds No. multiplied by dimensionless surface tension,

$\frac{\sigma}{\sigma_{ref}}$; viscosity, $\frac{\mu}{\mu_{ref}}$; and density, $\frac{\rho}{\rho_{ref}}$. The reference being water at 32 degrees F. It was found that all three corrections, with some manipulation, could correlate the data well. The corrections were $(\frac{\rho}{\rho_{ref}})^{2.5}$, $(\frac{\mu}{\mu_{ref}})$, and $(\frac{\sigma}{\sigma_{ref}})^{0.5}$ respectively for density, viscosity and surface tension. The data are shown for both flow directions in Figures 14 through 19. The data were also plotted against Reynolds No. multiplied by Weber No., $\frac{\rho v^2 d}{\sigma g_c}$. This is shown in Figures 20 and 21 for both flow directions. The Reynolds No./Weber No. correction, from data manipulation, is $(Nwe^{0.6} Nre)^{0.15}$.

For the purposes of this study any one of the correlations would be acceptable for use. The average error between the experimental and calculated discharge coefficients was less than 5%. Further experimentation, beyond the scope of this work, is required to further resolve which parameters best correlate the data for other fluids and flow conditions.

The dimensionless density correction was used for this work. This property is more easily measured in the experimental system and is more readily incorporated into the mathematical representation of the system. The correlation

Figure 14. Plot Depicting Effect of Dimensionless Density on Orifice Coefficient for Forward Flow Through Nozzles

Figure 15. Plot Depicting Effect of Dimensionless Viscosity on Orifice Coefficient for Forward Flow Through Nozzles

Figure 16. Plot Depicting Effect of Dimensionless Surface Tension on Orifice Coefficient for Forward Flow Through Nozzles

Figure 17. Plot Depicting Effect of Dimensionless Density on Orifice Coefficient for Reverse Flow Through Nozzles

Figure 18. Plot Depicting Effect of Dimensionless Viscosity on Orifice Coefficient for Reverse Flow Through Nozzles

Figure 19. Plot Depicting Effect of Dimensionless Surface Tension on Orifice Coefficient for Reverse Flow Through Nozzles

Figure 20. Plot Depicting Effect of Weber No. on Orifice Coefficient for Forward Flow Through Nozzles

Figure 21. Plot Depicting Effect of Weber No. on Orifice Coefficient for Reverse Flow Through Nozzles

incorporated into the pulser system model for forward flow through the nozzles is:

$$C = 1.318 - 9.06 \left(\frac{\rho}{\rho_{ref}} \right) \frac{1}{\sqrt{Nre}}$$

This is a relatively accurate correlation of the data. The standard error of the C estimate is 0.021 and the coefficient of determination is 0.980. The dimensionless density correction is slightly different than that shown above. Further data manipulation revealed that slightly better statistical results could be attained by removing the exponent and multiplying it by the reciprocal of the square root of the Reynolds No. Calculated and experimental values are shown in Figure 22. For reverse flow through the nozzles

$$C = 1.149 - 6.131 \left(\frac{\rho}{\rho_{ref}} \right) \left(\frac{1}{\sqrt{Nre}} \right), \quad \text{for} \quad \left(\frac{\rho}{\rho_{ref}} \right) \left(\frac{1}{\sqrt{Nre}} \right) > 0.033$$

$$C = 0.941, \quad \text{for} \quad \left(\frac{\rho}{\rho_{ref}} \right) \left(\frac{1}{\sqrt{Nre}} \right) < 0.033$$

Two correlations are needed for reverse flow through the nozzles to account for the coefficient's approximate constant value above a Reynolds No. of 1000. The standard error of the C estimate for these correlations is 0.015. The coefficient of determination is 0.965 for the sloped portion. The correlation is similar to that for forward flow through the nozzles. Calculated and experimental values are shown in Figure 23.

Figure 22. Plot of Orifice Coefficient Correlation
Used in Pulse Column Model for Forward
Flow Through Nozzles

Figure 23. Plot of Orifice Coefficient Correlation
Used in Pulse Column Model for Reverse
Flow Through Nozzles

A typical plot of the pulse leg pressure measured during pulse column testing is shown in Figure 24. Typical plots of the pulse leg amplitude and organic feed inlet line amplitude versus reservoir air pressure are shown in Figures 25 and 26 respectively. All data which were collected are reported in Appendix B.

As discussed previously, the mathematical representation of the system was solved using the computer software code ACSL. A discussion and listing of the code is reported in Appendix C. Isothermal and adiabatic simulations of the pulse leg pressure-time relationship for a pulse frequency of 50 cycles per minute (CPM) and a reservoir pressure of 10 PSIG are shown in Figure 27. The column and associated piping were filled with water. This is considered very close agreement. The pressure-time relationship at this frequency approaches a square wave. The measured data do not exactly match the simulation data because the simulation communication interval could not be matched exactly with the measurement interval. Further examination shows that the pressure wave is rounded on all edges, and that the pressure is not exhausted completely to atmospheric conditions.

The rounding effect is caused by several factors. Volume capacity of the system is the major contributor as the pressure approaches its peak value during pressurization and as the pressure approaches its minimum value during exhaust. The volume of the conduit connecting the pulser and the pulse

Figure 24. Variation of Pulse Leg Pressure with Time at a Reservoir Pressure of 10 PSIG and the Column Filled with Water

Figure 25. Plot of Pulse Leg Amplitude/Reservoir Pressure Relationship for a Frequency of 50 CPM

Figure 26. Plot of Organic Inlet Line Amplitude
Amplitude/Reservoir Pressure Relationship
for a Frequency of 50 CPM

Figure 27. Comparison of Measured and Simulated Pulse Leg Pressure-Time Relationships for a Reservoir Pressure of 10 PSIG and a Frequency of 50 CPM

leg was added to the rotary disc pulser volume in the adiabatic model. Air leakage around the pulser rotating discs has some effect at these points but is the significant contributor just prior to the initiation of pressure or exhaust. As the discs rotate and the port sections in the discs approach the vent or supply connection, the resistance to flow either increases or decreases depending on the position in the pulse cycle.

An optimization procedure was performed in order to achieve the excellent agreement between theory and measurement shown in Figure 27. Initially, the resistances in the air line sections and an estimated area available for air leakage around the discs were used in the model. The resistance to air leakage around the discs (A in Equations 22a and 22b) was then adjusted to approximate the measured peak and minimum air pressures in the pulse leg. This experimental value represented a gap of about one-half of the original estimate. The filter resistance was then adjusted such that peak air pressures observed were matched. Final values for A and the filter resistance are slightly different between the isothermal and adiabatic models. This is expected due to the thermodynamic relationship between temperature and pressure. These are listed in Appendix D. Finally, A in Equations 22a and 22b is increased slightly with each time increment to account for peak pressure decrease just prior to the exhaust portion of the cycle and

minimum pressure increase just prior to the pressurization phase of the cycle. Some peak pressure fluctuation, or "ringing", was noted during initial simulations. This was eliminated by increasing the resistance of the pulser itself which is added to the line connecting the pulse leg and the pulser.

Pressure-time relationships for 30 CPM-4 PSIG, 70 CPM-12 PSIG, and 90 CPM-14 PSIG are similar to Figure 27 and are shown in Figures 28, 29, and 30 respectively. Examination of these figures reveals that the isothermal model is essentially in total agreement with the adiabatic model. There is still some pressure fluctuation at the peak pulse leg pressure for 30 CPM for the simulations however. At higher frequencies and pressures the pressure-time relationship deviates from a square wave and begins to approximate a sinusoidal wave. This is expected. The isothermal and adiabatic simulations also deviate from each other in that the time required for peak pressure to be reached in the isothermal model is slightly greater. This deviation could be reduced by decreasing the resistance for the conduit connecting the pulser and the pulse leg in the isothermal simulation. The magnitude of deviation is quite small but is expected for higher pressures. Overall agreement between observed and calculated pressure-time relationships appears very good. This indicates that the assumption that air leakage around the rotating discs, with

Figure 28. Comparison of Measured and Simulated Pulse Leg Pressure-Time Relationships at a Reservoir Pressure of 4 PSIG and a Frequency of 30 CPM

Figure 29. Comparison of Measured and Simulated Pulse Leg Pressure-Time Relationships at a Reservoir Pressure of 12 PSIG and a Frequency of 70 CPM

Figure 30. Comparison of Measured and Simulated Pulse Leg Pressure-Time Relationships at a Reservoir Pressure of 14 PSIG and a Frequency of 90 CPM

some manipulation of the resistance, A , can be modelled adequately by flow through a nozzle. Additionally, the assumption that a mean density over a section of conduit, reevaluated at each time increment in the simulation, is valid.

Measured average pulse leg temperature versus reservoir pressure at different frequencies for both water and organic liquids in the pulse column system are shown in Figures 31 and 32 respectively. These data indicate that the pulse leg temperature is a relatively linear function of the applied pressure. Calculated and measured temperatures at several reservoir pressures for frequencies of 50 and 90 CPM with water in the pulse column system are shown in Figure 33. These two cases are typical of all the data. Excellent agreement is noted between theory and observed values at a frequency of 50 CPM, but a significant deviation is seen at 90 CPM. This is thought to be due to the experimental procedure and no heat transfer accounted for in the model. The piping acts as a heat sink, biasing the temperature measurement. By measuring data at higher pressures and then continuing to lower pressures the system retains the characteristics of the higher pressures. However, it has essentially no effect on pulse leg pressure simulation as can be seen in Figures 27 through 30 and is therefore believed to be insignificant.

A typical plot of pressure loss, calculated by the

Figure 31. Relationship Between Average Measured Pulse Leg Temperature and Applied Reservoir Pressure with Water in the Column

Figure 32. Relationship Between Average Measured Pulse Leg Temperature and Applied Reservoir Pressure with Organic in the Column

Figure 33. Comparison of Average Measured and Calculated Pulse Leg Temperatures with Water in the Column

Figure 34. Relative Contributions of Frictional, Accelerational, and Static Pressure Loss at a Reservoir Pressure of 14 PSIG A Frequency of 70 CPM, and Organic in the Column

simulation, versus time showing the relative contributions of pressure loss due to friction in the pulse leg, friction in the column plates, total accelerational loss, and static head is shown in Figure 34. When added together the total must equal the air pressure in the pulse leg, P_T . It can be seen that the major contributors are pressure loss due to acceleration of the liquid and static weight of the liquid. A significant unknown in the calculation of the acceleration loss is the distance that the fluid is accelerated, for the free column cross-sectional area, as it passes through the plates. The distance is thought to be between the thickness of the plate, 0.125 inches, and a maximum of about 0.75 inches. This is based on observation of two phase column operation. It appears to be dependent on the magnitude of the pulse leg amplitude, which indicates it is a function of liquid acceleration. Experimental determination of this function is beyond the scope of this work. Determination of constants for the accelerational terms is shown in Appendix D.

Measured and adiabatically calculated pulse leg liquid amplitudes at various reservoir pressures and frequencies are shown in Figures 35 and 36 for water and organic in the system respectively. Additionally, the organic inlet valve was closed. The solid lines represent calculated values. Close examination of these data reveal that the simulation predicts, for a particular frequency, higher values than

Figure 35. Comparison of Measured and Calculated Pulse Leg Amplitudes with Water in the Column

Figure 36. Comparison of Measured and Calculated Pulse Leg Amplitudes with Organic in the Column

observed for lower amplitudes and lower values than observed at higher amplitudes. The calculated values are based on an acceleration distance through the plates of 0.75 inches. The error range between experimental and calculated values is -4.5 to 23.3%. This indicates that the model does not predict enough pressure loss at lower accelerations and velocities and predicts too much at higher velocities and accelerations. Amplitudes were recalculated at reservoir pressures of 8 and 16 PSIG for a frequency of 70 CPM with an acceleration distance of 0.125 inches. This was for water in the column and where the error range was greatest. The error range changed to -11.2 to 17.2%.

At higher frequencies and pulse leg amplitudes it was noted that there was a significant amount of liquid held up on the pulse leg walls and was frothy at the air/liquid interface. It was believed that these phenomena were a significant contributor to the difference between observed and calculated values at high amplitudes. Therefore, the 1/2-inch glass pulse leg was exchanged for a 1-inch diameter pulse leg. Measured and calculated amplitudes at various reservoir pressures with a 1-inch pulse leg are shown in Figure 37. These data are with water in the column and the organic inlet closed. The calculated values are based on an acceleration distance through the plates of 0.25 inches. The error range is -12.6 to 8.8%. This is for 2 data points only, the other 13 are less than 7%. The accuracy of the

Figure 37. Comparison of Measured and Calculated Pulse Leg Amplitudes for a 1-Inch Pulse Leg with Water in the Column

simulation is improved considerably indicating that wall effects at higher amplitudes in the smaller diameter pulse leg are significant. The same effects with respect to higher calculated values at lower amplitudes and lower calculated values at higher amplitudes as observed in the smaller pulse leg are apparent however.

Two explanations are offered as possible causes for the discrepancy. The geometry of the perforated plates, with the nozzle configuration, could be causing an accelerational effect not being accounted for. It may be similar to the difference noted for steady frictional pressure loss through the plates. The system may also be passing through different flow regimes at the ends of the amplitude ranges where the assumption of a steady flow friction factor, even though it is recalculated over small increments of time, is invalid.

A comparison of the measured versus adiabatically calculated average pulse leg height is shown in Figure 38. Symbols represent measured data points and the lines represent calculated values. It is again apparent that predicted values differ from measured values at higher pressures and frequencies. The perforated plate nozzles are oriented towards the top of the column. Resistance to flow back through the nozzles not accounted for may be the reason for higher calculated average pulse leg heights. These phenomena support plate geometry causing discrepancies between measured and calculated pulse leg amplitudes.

Figure 38. Comparison of Measured and Calculated Average Pulse Leg Height for a 1-Inch Pulse Leg with Water in the Column

Table 4. Comparison of Measured, Adiabatic, and Isothermal Pulse Leg Amplitudes

	Reser		Pulse Leg	Amplitude		
Freq	Press	Meas	Adia	%Dev	Iso	%Dev
Water in System						
30	4	15.25	14.67	3.8	14.82	2.8
50	10	17.88	16.61	7.1	16.45	8.0
70	12	10.75	11.35	-5.6	11.06	-2.9
90	14	10.50	8.92	15.0	8.31	20.9
Organic in System						
30	6	30.50	27.89	8.6	27.53	9.7
50	10	23.25	21.00	9.7	20.59	11.4
70	12	16.75	15.21	9.2	14.62	12.7
90	14	13.75	12.35	10.2	11.55	16.0

A comparison of measured amplitudes and those calculated by adiabatic and isothermal simulations is listed in Table 4. The data are for the organic inlet valve closed. Both calculations are for the 1/2-inch diameter pulse leg and an accelerational distance of 0.75 inches through the plates. It can be seen that as frequency and pressure increase the deviation between the adiabatic and isothermal amplitude predictions increase. This is expected since temperature is a greater influence at higher pressures.

Comparison of this method of amplitude calculation to previous methods^{5,6,8,16,17} is difficult for two reasons. Previous methods assume that the peak pressure in the reservoir is delivered to the pulse leg, which is not the

case here. Previously published work also does not adequately describe equipment used to generate the data, which makes it impossible to adequately model the system with this method. However, the method of Kaser⁸ has been applied to the data generated by this work using measured peak pulse leg pressures. A constant orifice discharge coefficient of 0.75, as used by Kaser, was used in the calculation. The comparison for water in the system and a 1-inch pulse leg is shown in Figure 39. Kaser reports a maximum error between measured and calculated values of 7.5%. This was for a very similar experimental system filled with water and a frequency of 65 CPM. The maximum error when his method is applied to this work at 60 CPM is 28%. It is concluded that the rotary disc pulser produces a pressure-time relationship incompatible with the method or that there is some basic configurational difference.

Measured and adiabatically calculated pulse leg amplitudes at various reservoir pressures and pulse frequencies with the organic inlet valve open are shown in Figures 40 and 41. These data were measured in the 1/2-inch pulse leg. The same general trend noted previously can be seen here. Measured and adiabatically calculated amplitudes in the organic inlet line at various reservoir pressures and frequencies is shown Figures 42 and 43. The lines represent calculated values. These data coincide with the data in Figures 40 and 41. Actual column amplitudes, the parameter

Figure 39. Comparison of Measured and Calculated, by the Method of Kaser, Pulse Leg Amplitudes for a 1-Inch Pulse Leg and Water in the Column

Figure 40. Comparison of Measured and Calculated Pulse Leg Amplitudes with the Organic Inlet Valve Open and Water in the Column

Figure 41. Comparison of Measured and Calculated Pulse Leg Amplitudes with the Organic Inlet Valve Open and Organic in the Column

Figure 42. Comparison of Measured and Calculated Organic Inlet Line Amplitudes with Water in the Column

Figure 43. Comparison of Measured and Calculated Organic Inlet Line Amplitudes with Organic in the Column

we are most interested in, are determined using a simple mass balance.

$$a_c = a_L \frac{A_L}{A_c} - a_I \frac{A_I}{A_c}$$

Where subscript c represents the column, L the pulse leg, and I the organic inlet line. Upper case A is area and lower case a is amplitude. Measured and adiabatically calculated column amplitudes with the organic inlet open, a frequency of 70 CPM, and organic in the column is shown in Figure 44. Maximum error is 22% between the measured and calculated values, similar to that noted for data with the organic inlet closed. The error would be reduced considerably if wall effects were not present. The column amplitude is reduced by about 12%, for this experimental configuration, when the organic inlet is open.

A plot of measured pulse leg amplitude at a frequency of 50 CPM showing the effect of density is displayed in Figure 45. The difference between the water and organic densities has a significant effect on pulse leg amplitude. Actual column amplitude may change significantly during normal two phase column operation depending on dispersed phase holdup. A comparison of measured and adiabatically calculated pulse leg amplitudes for two phase operation is listed in Table 5. The calculated values are based on an acceleration distance through the plates of 0.25 inches. Operational conditions were aqueous continuous, total volume velocity of approximately 1000 gallons per hour per square foot of column

Figure 44. Comparison of Measured and Calculated Column Amplitude for a Frequency of 70 CPM with Organic in the Column

Figure 45. Plot Showing Effect of Density on Pulse Leg Amplitude for a Frequency of 50 CPM

Table 5. Comparison of Measured and Calculated Pulse Leg Amplitudes with Two Phase Operation and a 1-Inch Pulse Leg

Freq	Reser Press	Column Density	Pulse Leg Amplitude		
			Meas	Calc	%Dev
40	5	55.68	6.00	5.75	4.2
50	6	57.08	5.75	4.74	0.2
60	5	59.36	2.75	2.77	-0.7
70	6	59.36	2.38	2.52	-5.9

cross-sectional area, and an aqueous to organic flow ratio of 1. A static weight measurement was used to determine the column density which was used in the simulation. The holdup of the dispersed phase, organic in this case, was determined using the following:

$$\rho_m = \rho_o x_H + \rho_A (1 - x_H)$$

where x_H is the dispersed phase holdup, subscript m represents the column mixture, o the organic phase, and A the aqueous phase. The mixed phase viscosity used was calculated using the dispersed phase holdup and the following:

$$\mu_m = \mu_o x_H + \mu_A (1 - x_H)$$

These data are considered typical for normal column operation over these frequencies and amplitudes. The accuracy of the calculated amplitudes ranges from -5.9% to 4.2%. This appears to be good agreement and indicates that the two calculational assumptions are valid over the experimental range. These are an acceleration distance through the plates of 0.25 inches and a column liquid mixture viscosity based on fractional dispersed phase holdup.

CONCLUSIONS

The following conclusions were based on theoretical evaluations and experimental data.

Determination and simulation of the pressure-time relationship for the rotary disc pulser was the major goal of this work. The relationship can be simulated quite accurately, with knowledge of the physical configuration of the piping and a pulse leg pressure measurement, with the model presented here. The needed pressure information is available from existing plant instrumentation and pipe configurations are available from engineering drawings. The adiabatic model provides an almost imperceptibly more accurate simulation of the pressure-time relationship (Figures 27-30) than the isothermal model. The increased accuracy of the adiabatic model is more apparent when comparing amplitude calculations. It is about 5% more accurate at higher frequencies and pressures than the isothermal model. The assumption of an ideal gas appears accurate, but the system is obviously not isothermal (Figures 31 and 32). The adiabatic model appears to be superior to the isothermal model and additional computer time is negligible.

It has been shown that the nozzle plate orifice discharge

coefficient in a pulsed column system can be approximated as a function of Reynolds No. and dimensionless density correction to within 5%. It is obviously apparent that the assumption of a constant discharge coefficient is invalid. Knowledge of the value of the coefficient as a function of free area velocity and flow direction can increase pulse leg amplitude accuracy by approximately 10% since it's value can change by a factor of about 2 (Figures 22 and 23).

Design and plant operation of pulse columns centers almost exclusively between a frequency-column amplitude product of 45-75 inches per minute with frequencies of 40 to 70 CPM. The model presented here easily predicts column amplitudes in this range well within 10% (usually within 5%) for a 1-inch diameter pulse leg and within 12% for a 1/2-inch diameter pulse leg. Pulse leg diameters of greater than 1/2-inch should be used where possible to preclude wall wetting effects. The best estimate of the fluid accelerational distance through nozzle plates is 0.25 inches. Also, it appears that a column liquid mixture viscosity estimate based on a fractional dispersed phase holdup is valid for the range of experimental conditions tested. These assumptions will supply accuracy within the stated 10% in a plant operational range.

Addition of the organic phase to the pulse leg does not unduly affect pulse column amplitude and can be simulated effectively with the model presented herein. An average 12%

decrease in column amplitude is noted with this experimental setup.

Although not addressed in this work, it is believed that the model presented herein is easily adapted to other types of systems. Examples of these are organic continuous operation and poppet valve air pulsers. Only slight changes would be required to the model and the same operational characteristics apply to the equipment.

RECOMMENDATIONS

The following recommendations are based on analysis of data gathered as a result of this work.

It is recommended that further experimentation be initiated to determine the physical property parameter causing the deviation between aqueous and organic discharge coefficients. Knowledge of this phenomenon could be important in unrelated systems. Additional plate types should be tested to determine their discharge coefficient relationships. These data could improve amplitude estimates for pulse columns in which they are installed.

Further experimentation is recommended to determine why pressure drops are overestimated at low amplitudes and underestimated at high amplitudes. This information would be important to different cartridge designs requiring amplitudes outside the range now utilized. It is possible that the acceleration distance through the plates is related to this phenomenon. This estimate could certainly be refined.

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APPENDIX A
DESIGN CALCULATIONS FOR ROTARY DISC
AIR PULSER

Two problems were identified during development of the rotary disc pulser. One was mechanical since the rotating discs tend to deform from the applied air pressure. The other was to provide adequate flow area during pressurization and venting. The following discussion provides the background for choosing the pulser configuration.

The disc flow areas are schematically depicted in Figure 46. The enclosure size should be minimized and the flow area maximized. Open flow area as a function of time for various enclosures sizes was calculated as follows.

$$S = R \ 2\pi \omega \ t$$

$$h = R1 - R2$$

$$\text{Area} = \frac{1}{2} h (S1 + S2)$$

Where t is time and ω is rotation frequency in cycles per

Figure 46. Model of Air Flow Through a Disc

second. ω was chosen to be 0.67 (40 cycles/minute) to make the evaluation. This is usually the lower limit during operation. The results of the calculations are shown in Figure 47. A 10 inch enclosure was chosen since the open flow area at 0.02 second was 0.85 square inches vs. 0.22 and 0.5 square inches for a 6 inch and 8 inch enclosure respectively. This is close to the flow area of a 1" Sch. 40 pipe, which is used as the connecting conduit.

The mechanical deformation problem is represented in Figure 48. Disc deflection is a result of a uniform pressure load P , and can be represented by the following:¹²

$$\text{Deflection } y_a = M_{rb} \frac{a^2}{D} C_2 + Q_b \frac{a^3}{D} C_3 - \frac{Pa^4}{D} L_{11}$$

$$\text{where } M_{rb} = -\frac{Pa^2}{C_8} \left(\frac{C_9}{2ab} (a^2 - r^2) - L_{17} \right)$$

$$Q_b = \frac{P}{2b} (a^2 - r^2)$$

$$D = \frac{E t^3}{12 (1 - \nu)}$$

E is the modulus of elasticity, ν is Poisson's ratio, t is the disc thickness, and 304L SS is the construction material.

$$\text{Also } C_2 = \frac{1}{4} \left(1 - \left(\frac{b}{a}\right)^2 \left(1 + 2 \ln \frac{a}{b} \right) \right)$$

$$C_3 = \frac{b}{4a} \left(\left(\left(\frac{b}{a}\right)^2 + 1 \right) \ln \frac{a}{b} + \left(\frac{b}{a}\right)^2 - 1 \right)$$

Figure 47. Available Flow Area for
Different Size Enclosures

Figure 48. Mechanical Disc Deflection Model

$$C_8 = \frac{1}{2} (1 + \nu + (1 - \nu) \left(\frac{b}{a}\right)^2)$$

$$C_9 = \frac{b}{a} \left(\frac{(1+\nu)}{2} \ln \frac{a}{b} + \frac{(1-\nu)}{4} \left(1 - \left(\frac{b}{a}\right)^2 \right) \right)$$

$$L_{11} = \frac{1}{64} \left(1 + 4\left(\frac{r}{a}\right)^2 - 5\left(\frac{r}{a}\right)^4 - 4\left(\frac{r}{a}\right)^2 \left(2 + \left(\frac{r}{a}\right)^2 \right) \ln \frac{a}{r} \right)$$

$$L_{17} = \frac{1}{4} \left(1 - \frac{1-\nu}{4} \left(1 - \left(\frac{r}{a}\right)^4 \right) - \left(\frac{r}{a}\right)^2 \left(1 + (1+\nu) \ln \frac{a}{r} \right) \right)$$

Calculational results at three separate pressures are portrayed in Figure 49. The clearance between rotational

Figure 49. Deflection of Different Size Discs as a Result of Applied Air Pressure

and stationary parts was assumed to be 0.005 inch. It was believed that deflection would not be a problem for the maximum pressure expected if a 1.5-inch disc thickness was utilized. The deflection for a 1.5-inch thick disc at 40 PSIG is 0.0003 inch.

The final fabricated rotary disc pulser includes two 8.750-inch diameter, 1.5-inch thick discs mounted on a multi-diameter shaft as shown in Figure 50. Each disc has a 3.7-inch by 90° port to allow flow into or out of the device. The discs are positioned on the shaft such that the open areas are located 180° apart. A 10-inch Sch. 160 pipe bored to 8.760 inches and 5.5 inches long provides the enclosure. Two 12-inch square end plates house bearings which support the shaft, provide pressure and vent pipe connections, and seal up the pulser are shown in Figure 51. Ports 0.5 inch deep with dimensions matching those of the rotating discs are located symmetrically around the pressure and vent connections. These ports provide an area for air flow during pressurization or venting cycles.

Figure 50. Details of Pulser Discs and Shaft

Figure 51. Details of the Pulser End Plates

APPENDIX B
DATA GATHERED IN SUPPORT OF ROTARY
DISC PULSER TESTING

Table 6. Orifice Coefficient Data Gathered for
Water with Forward Flow Through
the Nozzles

Measured Differential Pressure				
Flow Liters/Hour	(lb/ft ²)			Mean Delta P
	Run Number			
	4	8	12	
61.3	0.132	0.106	0.079	0.106
92.0	0.212	0.212	0.159	0.194
122.7	0.318	0.291	0.185	0.265
153.3	0.397	0.397	0.344	0.380
184.0	0.503	0.556	0.450	0.503
214.7	0.741	0.715	0.609	0.688
245.3	0.874	0.874	0.741	0.830
276.0	1.086	1.086	0.900	1.024
306.7	1.456	1.271	1.112	1.280
354.6	1.431	1.329	1.334	1.365
425.5	2.045	1.942	1.959	1.982
496.4	2.914	2.709	2.674	2.766
567.4	3.629	3.476	3.389	3.498
638.3	4.549	4.243	4.210	4.334
709.2	5.520	5.265	5.137	5.307
780.1	6.492	6.389	6.222	6.368
851.0	7.361	7.565	7.255	7.394
922.0	8.536	8.792	8.433	8.587
992.9	9.712	10.325	9.770	9.936
1063.8	11.041	11.603	11.174	11.273
1134.7	12.370	12.983		12.677

Table 7. Orifice Coefficient Data Gathered for
Water with Reverse Flow Through
the Nozzles

Flow Liters/Hour	Measured Differential Pressure (lb/ft ²)			Mean Delta P
	Run Number			
	3	7	11	
61.3	0.159	0.053	0.106	0.106
92.0	0.212	0.159	0.132	0.168
122.7	0.318	0.318	0.225	0.287
153.3	0.450	0.424	0.371	0.415
184.0	0.583	0.556	0.516	0.552
214.7	0.715	0.715	0.662	0.697
245.3	1.006	0.953	0.834	0.931
276.0	1.218	1.244	1.072	1.178
306.7	1.509	1.509	1.324	1.447
354.6	1.891	1.994	1.748	1.877
425.5	2.709	2.811	2.595	2.705
496.4	3.680	3.731	3.575	3.662
567.4	4.652	4.805	4.660	4.705
638.3	5.776	6.083	6.010	5.956
709.2	7.258	7.565	7.493	7.439
780.1	8.843	8.996	9.055	8.965
851.0	10.479	10.734	10.829	10.681
922.0	12.523	12.677		12.600
992.9	14.363	14.619		14.491

Table 8. Orifice Coefficient Data Gathered for Organic with Forward Flow Through the Nozzles

Measured Differential Pressure				
	(lb/ft ²)			
Flow	Run Number			Mean
Liters/Hour	1	4	8	Delta P
68.7	0.152	0.130	0.130	0.137
103.1	0.260	0.217	0.238	0.238
137.5	0.303	0.282	0.368	0.318
171.8	0.390	0.390	0.498	0.426
206.2	0.542	0.520	0.607	0.556
240.6	0.715	0.650	0.715	0.693
274.9	0.867	0.845	0.910	0.874
309.3	1.040	1.040	1.083	1.055
343.7	1.235	1.192	1.300	1.242
397.4	1.473	1.473	1.506	1.484
476.8	2.123	2.037	2.092	2.084
556.3	2.774	2.774	2.803	2.783
635.8	3.554	3.554	3.472	3.526
715.3	4.420	4.420	4.309	4.383
794.8	5.287	5.287	5.271	5.282
874.2	6.316	6.233	6.275	6.275
953.7	7.320	7.279	7.362	7.320
1033.2	8.575	8.492	8.533	8.533
1112.6	9.663	9.747	9.830	9.747
1192.1	11.085	11.169	11.169	11.141

Table 9. Orifice Coefficient Data Gathered for Organic with Reverse Flow Through the Nozzles

Flow Liters/Hour	Measured Differential Pressure (lb/ft ²)			Mean Delta P
	Run Number			
	1	4	8	
68.7	0.108	0.130	0.108	0.116
103.1	0.173	0.217	0.217	0.202
137.5	0.282	0.325	0.303	0.303
171.8	0.433	0.412	0.477	0.441
206.2	0.520	0.563	0.650	0.578
240.6	0.758	0.737	0.845	0.780
274.9	0.953	0.975	1.040	0.990
309.3	1.170	1.192	1.235	1.199
343.7	1.387	1.408	1.473	1.423
397.4	1.755	1.715	1.673	1.714
476.8	2.557	2.468	2.594	2.539
556.3	3.532	3.430	3.597	3.520
635.8	4.615	4.518	4.811	4.648
715.3	5.872	5.731	6.107	5.903
794.8	7.259	7.237	7.404	7.300
874.2	8.701	8.910	9.035	8.882
953.7	10.374	10.541	10.583	10.500
1033.2	12.298	12.382	12.675	12.452

Table 10. Physical Properties of Fluids Used in Orifice Discharge Coefficient Testing

Property	Water	Organic
Density (lbm/ft ³)	62.32	51.00
Viscosity (lbm/ft-sec)	0.00073	0.001122
Surface Tension (lbf/ft)	0.00502	0.00185

Table 11. Pulse Column Data Gathered with
a 1/2-Inch Pulse Leg and Organic
in the Column

Res Press	FREQ	Amplitude Organic Inlet Valves CLOSED		Amplitude Organic Inlet Valves OPEN		Amplitude Organic Inlet Line		Leg	Res
		AMP	AVG HT.	AMP	AVG HT.	AMP	AVG HT.	Temp	Temp
4	30	17.25	20.34	18.25	20.41	4.63	24.60	84.8	79.2
6	30	30.50	17.86	32.25	17.74	9.75	24.42	89.1	78.8
4	50	5.75	20.39	6.00	20.35	1.25	24.64	86.5	78.4
6	50	9.75	18.18	10.13	18.12	2.25	24.60	88.9	78.4
8	50	15.50	15.65	16.00	15.67	3.75	24.49	93.9	78.3
10	50	23.25	12.82	24.75	12.80	7.75	24.16	98.4	78.1
12	50	32.00	10.21	34.00	10.25	11.00	23.85	103.8	77.7
6	70	4.75	18.18	4.75	18.26	1.25	24.64	92.1	78.6
8	70	7.75	15.82	8.00	15.75	1.75	24.62	95.9	79.3
10	70	11.75	13.18	12.00	13.25	2.88	24.49	100.2	78.8
12	70	16.75	10.51	17.50	10.52	5.00	24.19	106.4	78.8
14	70	23.50	7.69	25.00	7.79	8.00	23.73	116.9	78.8
16	70	31.00	5.00	34.50	5.06	9.00	23.36	123.3	79.0
8	90	4.25	15.93	4.50	15.88	1.13	24.64	110.5	78.8
10	90	6.75	13.34	7.00	13.38	1.75	24.58	110.8	79.2
12	90	9.25	10.80	9.50	10.90	2.75	24.45	113.1	79.3
14	90	13.75	8.26	14.50	8.23	4.75	24.05	118.3	79.3
16	90	19.50	5.19	21.00	5.25	7.00	23.44	125.4	79.5

Density = 50.94 lbm/ft³ Viscosity = 0.001089 lbm/ft-sec

Table 12. Pulse Column Data Gathered with
a 1/2-Inch Pulse Leg and Water
in the Column

Res Press	FREQ	Amplitude Organic Inlet Valves CLOSED		Amplitude Organic Inlet Valves OPEN		Amplitude Organic Inlet Line		Leg Temp	Res Temp
		AMP	AVG HT.	AMP	AVG HT.	AMP	AVG HT.		
4	30	15.25	20.98	15.50	21.02	4.00	24.49	96.4	78.3
6	30	24.75	18.95	26.50	18.86	7.75	--	88.9	77.5
6	50	8.00	19.21	8.19	19.20	2.31	--	88.9	77.5
8	50	12.25	17.15	12.50	17.19	4.63	24.44	93.6	77.4
10	50	17.88	14.88	18.24	14.94	5.63	24.29	99.7	77.2
12	50	24.00	12.75	25.00	12.75	8.50	24.04	106.2	77.0
8	70	5.88	17.28	6.00	17.25	1.81	24.51	95.2	79.0
10	70	8.63	15.19	8.63	15.11	2.63	24.45	100.9	78.1
12	70	10.75	13.02	12.38	13.02	4.00	24.30	106.0	78.1
14	70	16.75	10.95	17.38	10.94	5.75	24.15	111.9	77.5
16	70	24.25	8.66	26.25	8.74	8.88	23.74	123.4	77.2
10	90	4.88	15.30	5.13	15.39	1.81	24.51	108.0	77.0
12	90	6.75	13.26	7.00	13.19	2.75	24.47	112.3	77.0
14	90	10.50	11.25	10.75	11.30	3.75	24.33	117.5	77.0
16	90	15.00	8.96	16.00	9.00	5.63	24.08	124.9	76.8

Density = 62.20 lbm/ft³ Viscosity = 0.000580 lbm/ft-sec

Table 13. Pulse Column Data Gathered with
a 1-Inch Pulse Leg and Water
in the Column

Res Press	FREQ	Amplitude Organic Inlet Valves CLOSED		Amplitude Organic Inlet Valves OPEN		Amplitude Organic Inlet Line		Leg Temp	Res Temp
		AMP	AVG HT.	AMP	AVG HT.	AMP	AVG HT.		
4	40	4.13	21.06	4.50	21.02	4.50	24.69	92.5	87.6
6	40	6.50	18.98	6.87	19.05	7.87	24.46	96.8	87.0
8	40	8.63	16.74	9.25	16.76	13.00	24.15	100.8	87.0
4	60	1.75	21.14	1.87	21.13	2.13	24.79	95.9	87.0
6	60	2.87	19.17	3.13	19.20	3.50	24.65	99.0	87.0
8	60	4.13	17.11	4.38	17.07	5.38	24.46	103.1	87.0
10	60	5.25	14.86	5.63	14.85	7.75	24.22	107.6	87.0
12	60	6.38	12.71	6.87	12.69	10.63	23.89	111.2	87.0
14	60	7.50	10.50	—	—	—	—	113.9	87.0
8	80	2.25	17.30	2.25	17.32	3.00	24.65	108.5	87.0
10	80	2.87	15.26	3.00	15.23	4.25	24.49	112.1	87.0
12	80	3.75	13.09	3.87	13.13	5.63	24.29	117.5	87.0
14	80	4.50	10.94	4.75	11.01	7.75	24.05	119.3	87.0
16	80	5.25	8.76	5.75	8.76	9.75	23.80	120.6	87.0
18	80	6.00	6.50	—	—	—	—	120.0	87.0

Density = 62.13 lbm/ft³ Viscosity = 0.000531 lbm/ft-sec

Table 14. Pulse Column Data Gathered with
a 1-Inch Pulse Leg and Two Phase
Operation

Freq (CPM)	Reser Press (PSIG)	Pulse Leg		Temperature Degrees F		Static Weight (PSI)
		Amp (inches)	Avg Height (ft)	Reser	Pulse Leg	
40	5	6.00	22.00	79	87.8	10.71
50	6	4.75	20.80	79	91.4	10.95
60	5	2.75	23.65	79	87.0	11.34
70	6	2.38	22.48	79	90.1	11.34

	Density (lbm/ft ³)	Viscosity (lbm/ft-sec)
Aqueous	62.21	0.000595
Organic	50.87	0.001120

APPENDIX C

DISCUSSION AND LISTING OF COMPUTER CODE

USED TO SOLVE THE ROTARY DISC

PULSER MATHEMATICAL MODEL

A portion of the simulation code for this work had to be manipulated to account for rotation of the disc pulser, and hence pressurization and exhaust of the pulse leg. As the discs rotate, either air pressurizes the pulse leg or is exhausted from the leg. During this process air escapes around the exhaust disc into the vent during pressurization or is forced around the pressure disc into the pulser enclosure during the exhaust phase. The air accomplishes work on the liquid as it pressurizes the pulse leg and the liquid performs work on the air as it exhausts the pulse leg.

A sine wave function dependent on frequency is used to account for the change in air flow direction during pressurization or exhaust. When the sign of the function value is positive, the driving pressure differential is between the air reservoir and the pulse leg; the pressure differential is changed to that between the vent and the pulse leg when the function value is negative. Pressure differential terms for the air leakage terms in Equations 22a and 22b are handled in the same manner. Temperature, T , in Equations 22a and 22b is changed between temperature of the reservoir, T_R , and temperature in the pulser, T_p , similarly for the adiabatic model. The expansion/contraction coefficient in Equation 8 is changed according to the sign of the air velocity, v , in the isothermal model. Expressions for the pulser air temperature, T_p , (Equations 25 and 26)

and the pulse leg air temperature, T_1 , (Equations 28 and 31) are also switched according the sign of the air velocity, v , in the adiabatic model. Expressions for the work terms (Equations 29 and 30) are switched according to the sign of the liquid velocity in the pulse leg in the adiabatic model.

All frictional resistances to air or liquid flow in the calculations are in terms of equivalent lengths of pipe. The friction factor, f , is a function of Reynolds No. and changes with velocity. A new friction factor is calculated for each air and liquid section at each time step to account for this phenomena. Equivalent pipe length is also recalculated at each time step according to:⁴

$$L_e = \frac{K D}{f}$$

where D is the pipe diameter. Values of K for each air or liquid pipe section are determined by assuming turbulent flow in the section. The determination of these values and other constants are shown in Appendix D. Two resistances, or values of K , must be determined either through operation or experimentation. They are the resistance associated with the filter in the air line supplying the pulser and the resistance of the rotary disc pulser itself. The resistance of the filter changes as particulate builds up on the filter, which ultimately affects the air pressure observed in the pulse leg. Actual machined dimensions affect the rotary pulser resistance. The resistance to air leakage around the discs is also affected by actual machined dimensions.

The calculation of the frictional pressure drop through the column plates, in the liquid section of the model, requires a change in the method for determining the plate orifice discharge coefficient. Determination of the plate orifice discharge coefficient is keyed on, and switched by, the sign of the liquid velocity in the pulse leg.

A listing of the software code used for the calculations is available in Table 15.

Table 15. Listing of Computer Code Used with ACSL Software to Solve the Rotary Disc Pulser Mathematical Model

Program Amplitude Calculation for Air Pulsed Column

```
"-----Adiabatic version for ROTARY DISC PULSER SYSTEM  "
"-----Leakage simulated by flow through a nozzle with a  "
"-----variable area for pressure and vent                "
"----- Two leg system, disregarding organic inlet        "
"-----      1 inch Diameter (ID) Glass Pulse Leg         "
```

Initial

```
"-----Listing of constants for calculations "
Constant M=29.0, R=1545.0, GC=32.17, CP=0.241, CV=0.172
Constant A=0.006, AL=0.00545, AC=0.02183, AO=0.00457
Constant D=0.0874, DL=0.0833, H=24.67, J=778.0, R1=53.28
Constant L=11.5, KF=35.0, LL=27.75, FREQ=50.0, PATM=1771.2
Constant TR=545.0, KXE=33.0, N=112.0, RHOL=51.40, RHOM=60.55
Constant MUL=0.00134, MU=0.0000124, EP=0.00015, PR=3211.2
Constant A1=0.006, A2=0.006, D1=0.0874, D2=0.0874, MUM=0.001
Constant AP1=0.00071, AP2=0.00029, EP1=0.000005, VP=0.136
Constant L1=10.5, KF1=5.66, L2=2.0, KF2=1.72, RHOF=62.42
Constant INC1=1.0002, INC2=1.0012, THICK=0.0417, H6=21.0417
XIC = (PATM - (((0.9*PR-PATM)/2)+PATM) + RHOM*H) / RHOL
GAM = CP / CV
MTIC = (PATM * (LL-XIC)*AL) / (R1 * TR)
```

End \$ ' of Initial '

Dynamic

```
Cinterval Cint = 0.02923
Constant Tstp = 10.0
Termt(T .ge. Tstp)
```

Derivative

```
Algorithm Ialg = 5
Nsteps Nstp = 1
Maxinterval Maxt = 0.001
```

```
"-----MACRO to calculate the Friction factor needed for other  "
"-----calculations                                             "
```

```
MACRO FRIC(F,D,V,RHO,MUH,E)
MACRO REDEFINE RE,AA,BB
RE = ((D*RHO*ABS(V)) / MUH) + 10.0
AA = (2.457*ALOG(1.0/((7.0/RE)**0.9+(0.27*E/D))))**16.0
```

```

BB = (37530./RE)**16.0
F = (((8.0/RE)**12.0+1.0/(AA+BB)**(3./2.))**(1./12.))*2.0
MACRO END

FRIC(F = DL,XD,RHOL,MUL,EP1)
Xe = KXE*DL/F

"-----Solution of liquid equation to get pulse leg height, X"

B1 = (2.*F*RHOL*(XE+(X-3.42))) / (GC*DL)
B2 = ((N*RHOM)/(2.*GC**2.))*((AL/AO)**2.) * (1-(AO/AC)**2.)
B3 = (RHOM/GC) * (0.068 + N*THICK*(AL/AO) + ...
      (H6 - N*THICK)*(AL/AC))
B4 = (RHOL * (X+12.58)) / GC + B3
XDD = (PATM - PT + RHOM*H - RHOL*X - (B1 + B2)*XD*ABS(XD)) / B4
XD = INTEG(XDD,0.0)
X = INTEG(XD,XIC)

"-----Procedure to calculate the plate orifice coefficient  "

Procedural (C = XD)
  Nre = (ABS(XD+0.05) * (AL/AO) * .0156 * RHOM) / MUM
  COEFF = (RHOM/RHOF) * Nre**(-0.5)
  IF (XD .LT. 0.0) GO TO A5
  IF (COEFF .LT. 0.033) GO TO A22
  C = 1.149 - 6.131*COEFF
  GO TO A10
A22..C = 0.94
  GO TO A10
A5..C = 1.318 - 9.06*COEFF
A10..IF (C .LT. 0.1) C = 0.1
  Continue
End $ ' of Procedural '

"-----Calculation of Reynolds No., Friction factor, and velocity "
"-----for the section of pipe between the pulser and pulse leg  "

FRIC(F1 = D,VEL,RHOA,MU,EP)
LF = L + KF*D/F1

VELD1 = (2.0*F1*LF*RHOA*VEL*ABS(VEL)) / (GC*D)
VELD = (PP - PT - VELD1) / (RHOA*L/GC)
VEL = INTEG(VELD,0.0)

"-----Calculation of variables for further calculations "

RHOA = (((PP+PT)/2)*M) / (R*T3)
RHO1 = (((PR+PP)/2)*M) / (R*T1)

```

```

RHO2 = (((PP+PATM)/2)*M) / (R*T1)
MD1 = RHOA * VEL * A
T1 = (TR + TP) / 2
T3 = (TT + TP) / 2
MD2 = RHO2 * VEL2 * A2
MD3 = RHO1 * VEL1 * A1
VPL = AL * (LL - X)
MDD1 = (2.0*GC*GAM*M) / ((GAM-1.0)*R*T10)

"-----Calculation of pressure in the pulse leg "

MTD = MD1
MT = INTEG(MTD,MTIC)
PT = (MT * R1 * TT) / VPL

PT1 = PT/144. - 12.3    $ "---PT in psi and gauge "

"-----Calculation of temperature in the pulse leg "

TTD = (MD1*TTD1 + DW) / (MT*CV)
TT = INTEG(TTD,545.0)

TT1 = TT - 460.    $ "----Pulse leg temperature in deg F "

"-----Calculation of Reynolds No., Friction factor, and velocity "
"-----for the section of pipe between the air reservoir and the "
"-----pulser "

FRIC(F3 = D1,VEL1,RHO1,MU,EP)
LF1 = L1 + KF1*D1/F3

VEL1D1 = (2.0*F3*LF1*RHO1*VEL1*ABS(VEL1)) / (GC*D1)
VEL1D = (PR - B20 - VEL1D1) / (RHO1*L1/GC)
VEL1 = INTEG(VEL1D,0.0)

"-----Calculation of the pressure in the pulser, PP "

MPD = (-MD1 + MPD1 - FAC*MD12)
MP = INTEG(MPD,0.0083)
PP = (MP * R1 * TP) / VP

PP1 = PP/144.0 -12.3    $ "---PP in psi and gauge "

"-----Calculation of leakage into or out of pulser "

MD = AP*SQR(MDD1*(PRAT**((2.0/GAM) - PRAT**((GAM+1.0)/GAM)))
MD12 = MD * B20

```



```

"-----Calculation of temperature in the pulser "

      TPD = (MD1*TPD1 + MPD1*TPD2 + MD12*TPD3) / (MP*CV)
      TP = INTEG(TPD,545.0)

      TP1 = TP - 460.      $ "---Pulser temperature in degrees F "

"-----Calculation of Reynolds No., Friction factor, and velocity "
"-----for the section of pipe between the pulser and the vent "

      FRIC(F2 = D2,VEL2,RHO2,MU,EP)
      LF2 = L2 + KF2*D2/F2

      VEL2D2 = (2.0*F2*LF2*RHO2*VEL2*ABS(VEL2)) / (GC*D2)
      VEL2D = (PATM - B21 - VEL2D2) / (RHO2*L2/GC)
      VEL2 = INTEG(VEL2D,0.0)

"-----Section to simulate pressure/vent operation of pulser "

      SG = SIN(0.1047 * FREQ * T)
      SG1 = SIN(0.1047*(FREQ/2)*T)

Procedural (PRAT,MPD1,B20,B21,T10 = SG,PP,MD2,MD3,TP)
      IF (SG .LT. 0.0) GO TO L5
      PRAT = PATM / PP
      MPD1 = MD3
      FAC = 1.0
      B20 = PP
      B21 = PATM
      T10 = TP
      GO TO L10
L5..PRAT = PP / PR
      MPD1 = MD2
      FAC = -1.0
      B20 = PR
      B21 = PP
      T10 = TR
L10..CONTINUE
End $ ' of Procedural '

"-----Section to simulate pulse leg temperature and to account "
"-----for change in direction of velocity "

Procedural (TTD1 = TP,TT)
      IF (VEL .LT. 0.0) GO TO L15
      TTD1 = CP*TP - CV*TT
      GO TO L20

```

```

L15..TTD1 = CP*TT - CV*TT
L20..CONTINUE
End $ ' of Procedural '

"-----Section to simulate pulser temperature and to account "
"-----for change in direction of velocity "

Procedural (TPD1,TPD2,TPD3 = TP,TT,VEL)
  IF (SG .LT. 0.0) GO TO L35
  TPD1 = CV*TP - CP*TP
  IF (VEL .LT. 0.0) TPD1 = CV*TP - CP*TT
  TPD2 = CP*TR - CV*TP
  TPD3 = CV*TP - CP*TP
  GO TO L40
L35..TPD1 = CV*TP - CP*TT
  IF (VEL .GT. 0.0) TPD1 = CV*TP - CP*TP
  TPD2 = CP*TP - CV*TP
  TPD3 = CP*TR - CV*TP
L40..CONTINUE
End $ ' of Procedural '

"-----Section to account for work done by air on liquid or "
"-----for liquid on air "

Procedural (DW = X,XD,PT)
  IF (XD .GT. 0.0) GO TO L25
  DW = PT * AL * XD / J
  GO TO L30
L25..DW = (PATM + RHOM*H - RHOL*X) * AL * XD / J
L30..CONTINUE
End $ ' of Procedural '

"-----Section to simulate rounding of the pressure curve at "
"-----the end of pressurization and at the start of it "

Procedural (AP = SG,SG1,AP1,AP2)
  IF (SG .LT. 0.0) GO TO C20
  IF (ABS(SG1) .GT. 0.9) GO TO C10
  AP = AP1
  GO TO C50
C10..AP = INC1 * AP
  GO TO C50
C20..IF (ABS(SG1) .LT. 0.3) GO TO C30
  AP = AP2
  GO TO C50

```

```
C30..AP = INC2 * AP
C50..CONTINUE
End $ ' of Procedural '

End $ ' of Derivative '

End $ ' of Dynamic '

Terminal
    "-----Final Debug Dump"
    Logical      Dump
    Constant Dump = .False.
    If(Dump) Call Debug
End $ ' of Terminal '

End $ ' of Program '
```

APPENDIX D

LISTING OF CONSTANTS AND DETERMINATION OF

ACCELERATIONAL AND FRICTION TERMS

D_c : Diameter of pulse column in the plate, or working, section

$$2.00 \text{ inches} = 0.1667 \text{ feet}$$

A_c : Cross sectional area of the pulse column in the plate, or working, section = 0.02183 ft^2

A_f : Area available for flow in the plate section of the column

Plate Diameter=1.975 inches, Spacer Diameter=0.40 inches

Area taken up by Spacer: 0.00087 ft^2

Annular area between plate and column: 0.00055 ft^2

Free area of plate: 0.00489 ft^2

Free area = $0.00489 + 0.00055 - 0.00087 = 0.00457 \text{ ft}^2$

D_l : Pulse leg diameter:

$1/2 \text{ inch glass} = 0.0417 \text{ ft}$

$1 \text{ inch glass} = 0.0833 \text{ ft}$

A_l : Cross-sectional area of pulse leg:

$1/2 \text{ inch glass} = 0.00136 \text{ ft}^2$

$1 \text{ inch glass} = 0.00545 \text{ ft}^2$

D_i : Diameter of organic inlet line, $1/2 \text{ inch polyethylene tube} = 0.0313 \text{ ft}$.

A_i : Cross-sectional area of organic inlet line
= 0.00077 ft^2

H : Vertical distance from pulse leg inlet to column overflow = 24.67 ft

L_l : Vertical distance from the pulse leg connection to the one inch pipe at the top to the pulse leg entry into the column = 27.75 ft

N : Number of column plates = 112

Resistance of the pulse leg:

4 feet of 3/8 inch pipe, diameter = 0.0417 ft

Cross-sectional area = 0.00136 ft²

For turbulent flow and $\epsilon = 0.00015$ ft, then
friction factor $f_T = 0.00724$

Item	K	N	NK
Flow-through tee	$20f_T$	1	0.14
45 deg bend	$8f_T$	1	0.06
90 deg bend	$16f_T$	1	0.12
4 ft of pipe	$96f_T$	1	0.69
Exit/Entrance	1.0/0.78	1	0.89
Total			1.90

For the 1/2-inch pulse leg, $K(X_e) = 1.90$

1-inch pulse leg: $\beta = 0.5$ and $K(1) = \frac{K(1/2)}{\beta} = 30.4$

Expansion/Contraction = $0.563/0.43 = 0.5$
(Table 5-13, Reference 10)

For organic inlet closed, $K(1) = 30.4 + 0.5 \cong 31.0$

For organic inlet open, $X_2 = 3.083$, $K(X_{2e}) = 1.90$
and $K(X_e) = 0.5$

Resistance of organic inlet line:

1/2-inch stainless tube, diameter of 0.0358 ft

Cross sectional area of 0.00101 ft².

For turbulent flow and $\epsilon = 0.000005$ ft.,
then the friction factor, $f_T = 0.005$

Item	K	N	NK
Tee, Branch Flow	$60f_T$	2	0.60
Tee, Flow-through	$20f_T$	3	0.30
90 deg Elbows	$30f_T$	3	0.45
90 deg Elbows	$16f_T$	10	0.80
Ball Valves	$3f_T$	2	0.04
Unions	$0.04f_T$	2	0.08
Heat Exchangers	~1.0	1	1.00
28.42 ft of Tube	$793f_T$	1	3.97
Total			7.24

$$\beta = 0.375/0.430 = 0.872$$

$$K(1/2\text{-poly}) = K(1/2\text{-SS}) \beta^4 = 4.23$$

$$K(X_{le}) = 4.23, \text{ to account for horizontal length}$$

$$X_{le} = (X_l - 19.92) + \frac{K(X_{le}) D_l}{f}$$

D : Diameter of the pipe connecting the pulse leg and the pulser,

$$1 \text{ inch Sch. 40 pipe} = 0.0874 \text{ ft.}$$

A : Cross-sectional area of the pipe connecting the pulse leg and the pulser = 0.006 ft^2

L : Length of pipe connecting the pulse leg and the pulser = 11.5 ft

L_f : Pipe length plus equivalent length of fittings which contribute to friction loss in the pipe connecting the pulser and pulse leg, 1 inch Sch. 40 pipe.

For turbulent flow, and $\epsilon = 0.00015 \text{ ft.}$, then $f_T = 0.006$

Item	K	N	NK
90 deg Elbows	$30f_T$	2	0.36
45 deg Elbows	$16f_T$	2	0.20
Tee, Flow-through	$20f_T$	2	0.24
Unions	$0.04f_T$	2	0.08
Entrance/Exit	1.0	1	1.0
Total			1.88

However, the unknown pulser resistance must be added here. Found experimentally to be approximately 38, so $K_f = 40$

D_1 : Diameter of the pipe connecting air reservoir and the pulser

$$= \text{same as } D$$

A_1 : Cross-sectional area of the pipe connecting the air reservoir and the pulser

$$= \text{same as } A$$

L_1 : Length of pipe connecting the air reservoir and the pulser
 $= 10.5 \text{ ft}$

LF_1 : Pipe length plus equivalent length of fittings which contribute to friction loss in the pipe connecting the air reservoir and the pulser. Same assumptions as for LF .

Item	K	N	NK
90 deg Elbows	$30f_T$	4	0.72
Tee, Flow-through	$20f_T$	2	0.24
Tee, Branch Flow	$60f_T$	1	0.36
Unions	$0.04f_T$	3	0.12
Ball Valve	$3f_T$	1	0.02
Entrance	1.0	1	1.0
Total			2.46

However, a filter exists in this line for which the resistance changes as particulate builds up. It is varied to match pulse leg pressure.

$$K(\text{filter}) + 2.46 = KF_1$$

$$LF_1 = L_1 + \frac{KF_1 D_1}{f}$$

D_2 : Diameter of the pipe connecting the pulser and the vent
 $= \text{same as } D$

A_2 : Cross-sectional area of the pipe connecting the pulser and the vent
 $= \text{same as } A$

L_2 : Length of pipe connecting the pulser and the vent
 $= 2.0 \text{ ft}$

LF_2 : Line length plus equivalent length of fittings which contribute to friction loss in the pipe connecting the pulser and the vent. Same assumptions as L_f .

Item	K	N	NK
90 deg Elbows	$30f_T$	1	0.18
Unions	$0.04f_T$	1	0.04
Entrance	0.5	1	0.5
Exit	1.0	1	1.0
Total			1.72

$$K(F_2) = 1.72, \quad LF_2 = L_2 + \frac{K(F_2) D_2}{f}$$

A_p : Area available for, or resistance to air flow around the rotating discs in the pulser. Based on an estimate of a 0.006 inch gap, initially guessed to be 0.00116 ft²

Found experimentally to be:

	Pressure	Exhaust
Adiabatic Model	0.00071	0.00029
Isothermal Model	0.000725	0.00031
Correction Factor to Account for Increase or Decrease	1.0002	1.0012

V_p : Volume between the rotating discs inside the pulser plus the volume of the pipe connecting the pulser and the pulse leg.

$$0.067 + (0.006) 11.5 = 0.136 \text{ ft}^3$$

M : Molecular weight of air, $29 \frac{\text{lbm}}{\text{lb}_{\text{mol}}}$

R : Universal gas constant, $1545.0 \frac{\text{ft-lbf}}{\text{lb}_{\text{mol}}-\text{R}}$, or
for air $53.28 \frac{\text{ft-lbf}}{\text{lbm}-\text{R}}$

g_c : Gravitational constant, $32.17 \frac{\text{ft-lbm}}{\text{lbf-sec}^2}$

J : Energy conversion factor, $778 \frac{\text{ft-lbf}}{\text{BTU}}$

C_p : Heat capacity of air at constant pressure assumed to be constant at $0.241 \frac{\text{BTU}}{\text{lb}_m-\text{R}}$

C_v : Heat capacity of air at constant volume assumed to be constant at $0.172 \frac{\text{BTU}}{\text{lb}_m-\text{R}}$

K_c : Contraction coefficient at connection of pipe and pulse leg for 1/2-inch pulse leg = 0.449, (Table 5-13 of Reference 10.)

K_e : Expansion coefficient at connection of pipe and pulse leg for 1/2-inch pulse leg

$$\left[1 - \left(\frac{A_1}{A_2} \right) \right]^2 = 0.596$$

ϵ : Relative roughness of pipes. Assumed to be 0.000005 feet for glass and tube and 0.00015 feet for pipe.

Accelerational Terms:

For Column in Terms of X:

$$\text{General: } \frac{\rho_m H}{g_c} \left(\frac{A_L}{A_c} \right) \frac{d^2 X}{dt^2}$$

Adding Up All Sections:

$$\frac{\rho_m}{g_c} \left[H_1 \left(\frac{A_L}{0.3382} \right) + H_2 \left(\frac{A_L}{0.180} \right) + H_3 \left(\frac{A_L}{A_D} \right) + H_4 \left(\frac{A_L}{A_c} \right) + H_5 \left(\frac{A_L}{0.1091} \right) \right]$$

$$H_1 = 3.167, H_2 = 0.292, H_5 = 0.167$$

T = Thickness of plates, or total distance above and below plate fluid is accelerated to at the free cross-sectional area, A_f

$$H_6 = 21.0417 \text{ ft}; H_3 = NT, N = \# \text{ of plates}, H_4 = H_6 - H_3$$

$$\frac{\rho_m}{g_c} \left[0.068 + NT \left(\frac{A_L}{A_L} \right) + (H_6 - NT) \left(\frac{A_L}{A_L} \right) \right] \text{ for 1-inch pulse leg.}$$

0.068 is changed to 0.017 for 1/2-inch pulse leg.

For Pulse Leg in Terms of X:

$$\text{General: } \frac{\rho_L X}{G_c} \frac{d^2 X}{dt^2}$$

Adding Two Sections:

$$\frac{\rho_L}{g_c} \left[4 \left(\frac{A_L}{0.00136} \right) + (X - 3.42) \right]$$

$$\frac{\rho_L}{g_c} [16 + (X - 3.42)]$$

$$\frac{\rho_L}{g_c} [12.58 + X] \text{ for 1 inch diameter pulse leg.}$$

12.58 is changed to 0.67 for 1/2 inch pulse leg.

For The Organic Inlet Line:

General: $\frac{\rho_L X_I}{g_c}$

28.42 feet of 0.430 inch SS tube, 19.92 feet of vertical distance from pulse leg entry to top of tube.

$$\left[\frac{\rho_L (28.42)}{g_c} \left(\frac{0.00077}{0.00101} \right) + \frac{\rho_L}{g_c} (X_I - 19.92) \right] \frac{d^2 X_I}{dt^2}$$

$$\left[\frac{\rho_L}{g_c} (21.61 + X_I - 19.92) \right] \frac{d^2 X_I}{dt^2}$$

$$\frac{\rho_L}{g_c} (X_I + 1.69) \frac{d^2 X_I}{dt^2}$$