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THE PERFORMANCE OF TWO-PHASE NOZZLES
FOR TOTAL FLOW GEOTHERMAL IMPULSE TURBINES*

T. W. Alger

Lawrence Livermore Laboratory, University of California
Livermore, California 94550

The "Total Flow" impulse turbine concept requires the efficient conversion of thermal energy into kinetic energy for momentum transfer to an appropriately designed turbine wheel. For this reason, the flow of high pressure steam-water mixtures at low quality through converging-diverging nozzles was investigated.

Nozzle inlet qualities and pressures were varied over a range from approximately 10 to 20 percent and 1.8 to 2.8 MPa, respectively, to simulate geothermal wellhead products. The optimum nozzle performance for a specific geometry was determined by varying the nozzle back pressure from atmospheric to approximately 14 kPa. Experimental thrust coefficients and pressure profiles are presented with comparison to simple theory. Observations were made concerning nozzle exit shock and expansion waves when operating at off-design conditions. As anticipated, the optimum nozzle efficiencies have been high.

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INTRODUCTION

The concept of energy conversion from hot geothermal brines using the "Total Flow" impulse turbine (Austin, et al., 1973, Austin and Lundberg, 1974) has necessitated a study of the performance of two-phase high-velocity nozzles for the efficient acceleration of geothermal fluids. The physical requirement of high velocity for efficient conversion of thermal energy to kinetic energy for momentum transfer in an impulse turbine dictates the use of converging-diverging nozzles for supersonic flow velocities. This study presents experimental data and simple theoretical comparisons for the optimum and non-optimum performance of converging-diverging nozzles utilizing low-quality steam as the working fluid.

There has been a large amount of work accomplished towards the understanding of steam flow in supersonic nozzles in which initially superheated or saturated vapor is flowing, but there are relatively few references in the literature concerning information necessary for the design and performance of a supersonic nozzle operating in the low-quality steam region. Superheated steam nozzles can be designed quite satisfactorily using perfect gas principles, with corrections extending the analysis into the high-quality two-phase region. In the low-quality region such simplicity cannot properly model the flow since the complex interphase mass, momentum, and energy transfer mechanisms must be taken into account.

Probably the most comprehensive experimental information available for the design of low-quality steam nozzles is the result of several studies accomplished at the University of California at Berkeley in the early 1960's (Maneely, 1962;
Neusen, 1962; Brown, 1962; Starkman, et al., 1964). However, these works did not pursue the conditions necessary for the optimum performance of converging-diverging two-phase steam nozzles. Their main interest was the determination of mass flow rate for choked two-phase flow conditions. Their experimental data indicates that the nozzles tested were typically operating overexpanded (ambient pressure higher than ideal) and inefficiently, due to the shock waves occurring inside the nozzles. Thus, the thrust coefficients and inferred efficiencies were typically low for the operating conditions studied.

The performance of an impulse turbine is directly related to the efficiency of the major components, the nozzle and the wheel. Optimum nozzle thrust coefficients were found to be typically 0.92-0.94 for low-quality clean steam experiments. Thus, the optimum nozzle efficiency, which is essentially the square of the thrust coefficient, was typically 0.85 to 0.88.

Thrust coefficients are generally considered to deteriorate markedly when water droplets are present (Austin and Lundberg, 1974). However, the limited work presented here shows that high performance nozzles with thrust coefficients up to and probably beyond 0.94 can be designed for inlet quality (vapor mass fraction) as low as 12 percent.

EXPERIMENTAL NOZZLES

The data presented in this report were obtained using simple axisymmetric conical converging-diverging nozzles. A photograph of a typical nozzle is shown in Figure 1(a) with a rubber injection mold of the internal contour alongside for comparison. Figure 1(b) contains a photograph of a typical nozzle installed in the nozzle test chamber. Figure 2 presents the geometry and the numerical designations of the nozzles for which data are presented. The different nozzles were constructed to compare with the earlier results of Starkman (1964) and to study the effects of back pressure and length upon the nozzle performance.
EXPERIMENTAL APPARATUS

Geothermal Test Facility

In order to simulate geothermal wellhead products (using clean water), a test facility was constructed for the nozzle test station as depicted in Figure 3. A complete description of the test facility is given by Weiss and Shaw (1975).

Instrumentation

The pressure, temperature, and mass flow rate of the hot water were monitored upstream of the nozzle test chamber control valve while the fluid was in the single phase region. The mass flow rate was determined by measuring the pressure drop across an orifice that was designed according to the ASME specifications (Bean, 1971) and calibrated in place using a weigh tank arrangement. The estimated maximum error for the enthalpy and mass flow rate measurements was ±1% and ±1.5%, respectively.

The nozzle static pressure profiles were determined by measuring the pressure through 0.5 mm diameter holes in the nozzle wall. Transducer scales were varied according to the static pressure range to gain maximum accuracy. The estimated maximum error for the static pressure measurements was ±1%

The nozzle thrust, which is essentially a measure of the nozzle efficiency, was monitored in the location shown in Figure 3. The thrust transducer was thermally and barometrically compensated and flexibility was designed into the supply piping to minimize the introduction of error. The estimated maximum error encountered with the thrust measurements at the optimum operating conditions was near ± 2.5%. At the lower values of thrust, this error becomes proportionally greater.

Data Collection and Handling

With the enthalpy fixed at the particular state of the saturated liquid (state point 1, Figure 3), the nozzle inlet and exit conditions were varied by means of the control valve and condenser pressure, respectively. Nozzle inlet
quality and mass flow rate thus changed with pressure, but the inlet enthalpy
was constant for each series of tests. All data were recorded on magnetic tape
through a multichannel data handling system for computer reduction.

Visual Observations

The effect of different back pressures (condenser pressures) on the nozzle
exit jet flow pattern is shown in Figure 4. Part (a) concerns the jet flow when
the nozzle is operating overexpanded with a shock wave near the nozzle exit. The
most noticeable effect is the turbulence within the jet as a result of the abrupt
change in flow properties occurring within the shock wave. This condition
represents highly inefficient nozzle operation.

Figure 4(b) is an example of the jet flow of a nozzle operating at a near
optimum expansion. In contrast to part (a), the exiting flow pattern disturbance
is small and the jet boundary well defined. This jet flow pattern represents
efficient operation.

The nozzle jet flow pattern of Figure 4(c) occurs when the nozzle is operating
underexpanded. The jet boundary clearly indicates the expansion process occurring
at the nozzle exit to adjust for the lower back pressure. This condition again
represents inefficient operation since the total available thermal energy has not
been converted into the desired kinetic energy.

ANALYTICAL MODELS

Two different theoretical models were utilized for the prediction of the
genral nozzle design and performance, with a third model relating nozzle thrust
and efficiency. The first was used extensively for its simplicity and surprisingly
accurate predictions, and the second used to study the more complex nature of
the flow since it includes such models as interphase slip and droplet breakup.
All models assume that the flow is completely within the fog flow regime (small
liquid droplets dispersed within a sea of vapor).
**Isentropic Homogeneous Equilibrium Model**

From the simple theoretical model, termed the isentropic homogeneous equilibrium or IHE model by Starkman (1964), the fluid velocity at any desired downstream pressure within the nozzle can be calculated from the first law of thermodynamics as (assuming an isentropic no slip expansion to that pressure)

\[
\frac{V_s^2 - V_c^2}{2} = h_c - h_s
\]

i.e., the kinetic energy change is equal to the enthalpy change. The flow cross-sectional area can be calculated using the steady flow continuity equation based upon a mass averaged mixture density. The IHE model equations were solved with a computer program that used the computerized ASME steam tables as a subroutine for property determination (McC1into and Silvestri, 1968 and 1970).

**Numerical Model**

The second analytical tool utilized for general flow prediction was a one-dimensional, single-component computer program developed by Elliott and Weinberg (1968). As with the IHE model, this computer solution calculates the nozzle flow parameters as a function of downstream static pressure. This numerical solution includes the more complex effects of interphase mass and momentum transfer with a droplet breakup criterion. It also includes nozzle wall frictional effects (using a simple boundary layer model) but does not treat interphase energy transfer since thermal equilibrium is assumed. This numerical solution results with a much more complex and complete solution technique.

**Analytical Thrust and Efficiency Coefficients**

The thrust or velocity coefficient, which is a measure of the nozzle exit flow momentum, is defined as

\[
C_T = \frac{F}{\dot{m}_t V_{es}}
\]
where the nozzle thrust for the two phase fluid can be written as

\[ F = \dot{m}_v \, V_{ev} + \dot{m}_l \, V_{el} + (P_e - P_b) \, A_e \]

Thus, the determination of the thrust coefficient requires the simultaneous measurement of the nozzle thrust, mass flow rate, and inlet enthalpy.

In comparison, the nozzle efficiency coefficient can be written as

\[ \eta^2 = \frac{\dot{m}_v \, V_{ev}^2 + \dot{m}_l \, V_{el}^2}{\dot{m}_t \, V_{es}^2} \]

Upon introducing the slip ratio

\[ \beta = \frac{V_{el}}{V_{ev}} \]

and the mass flow rate ratio

\[ r = x/[x + (1-x) \, \beta] \]

the thrust and energy coefficients can be combined to yield (neglecting the pressure terms)

\[ \frac{\eta^2}{C_T} = \frac{[r + (1-r) \, \beta^2]}{[r + (1-r) \, \beta^2]^2} \]

From this relation it can be shown that \( \eta^2/C_T \geq 1.0 \) for all values of nozzle exit qualities and slip ratios. Thus, when the thrust coefficient pressure terms can be neglected (when operating at overexpanded or optimum conditions) the thrust coefficient becomes a measurement of the nozzle efficiency.

**DISCUSSION OF RESULTS**

The results of these initial experiments have been separated into two main areas for discussion; nozzle static pressure profiles and thrust coefficients. These results will be presented in the following two sections.

**Static Pressure Profiles**

The initial experiments were conducted and the data compared to the similar results of Starkman (1964). The comparison of a nozzle pressure profile for a similar inlet enthalpy is shown in Figure 5. Even though there are geometrical differences in the nozzle subsonic portions and a small difference between each
inlet enthalpy, excellent agreement is found between the two curves. The main discrepancy occurs near the nozzle exits where the shock waves are located. The different back pressures and the different flow conditions prior to the shock waves can explain the disagreement between the two experiments. Due to a lack of pressure taps in the region of the shock wave, the complete shock wave pressure profiles were not determined for several of the tests presented herein. This lack of data is represented by the dashed lines for the shock wave profiles in question. Also, the last data point of the pressure profile curves will always correspond to the nozzle back pressure. These representations will be used throughout this report on all concerned graphs.

Figure 6 is a graph of the static pressure profile for nozzle #2 at constant inlet enthalpy. Again, the different pressure profiles occurring near the nozzle exit are a result of the different shock wave conditions. This graph demonstrates that the pressure profile is determined by the inlet enthalpy (up to the location of the shock wave) rather than the plenum chamber quality as previously presented by Starkman (1964). The static pressure dependence upon enthalpy can be realized theoretically simply by neglecting the effects of interphase slip and writing the combined first and second laws of thermodynamics in differential form as

\[ Tds = dh - dp/\rho \]

For a isentropic process, this equation becomes

\[ dp = \rho dh \]

which demonstrates the direct dependence of static pressure upon enthalpy instead of the quality only.

Figure 7 is a graph of the different pressure profiles occurring for nozzle #2 under various back pressure conditions. These curves graphically depict the effect of back pressure upon the shock strength and location for a constant inlet enthalpy. The back pressure for optimum expansion for this test can be seen to be near 31.0 kPa.
A decrease in the back pressure below this value will result in an under-expanded nozzle condition, which is shown by the lower experimental curve. Underexpanded nozzle operation results with a pressure at the exit that is greater than the back pressure. Since this condition does not represent a complete expansion (all available thermal energy has not been converted into kinetic energy) the nozzle is inefficient when operating in this mode.

The pressure profile as predicted by the IHE model compares very well with the optimum experimental pressure profile of Figure 7. This comparison indicates the ability of the simple theory to predict reasonable results for the design of two-phase nozzles. However, the main disadvantage of the IHE model is the under-prediction of mass flow rate or overprediction of flow cross-sectional area, which is a consequence of the zero slip assumption (see Starkman, 1964). A pressure profile for this nozzle was also calculated using the computer program of Elliott and Weinberg (1968). This curve can be seen to have excellent agreement with the experimental data for the optimum pressure profile of this nozzle.

Figure 8 is a graph of the different pressure profiles occurring for nozzle #7 operating at constant inlet and various back pressure conditions. The pressure profiles are similar to those of Figure 7 except that the shock waves occur further downstream in the nozzle. Due to equipment limitations during this experiment, the optimum pressure profile was not determined. However, by extrapolation the optimum back pressure is near 13.8 kPa for the operating conditions presented.

Again the results of the IHE model and the numerical solution of Elliott and Weinberg (1968) are presented for comparison. Both calculational models predict the nozzle static pressure profile accurately. The IHE prediction agrees more closely with the experimental profiles near the nozzle exit where the effects of slip are small. However, the numerical prediction results with better overall agreement, especially in the throat region where the interphase slip is theoretically at a maximum.
Thrust Coefficients

Experimental thrust coefficients are presented in Figures 9 and 10 as a function of nozzle back pressure. The different curves demonstrate the effects of various nozzle inlet pressures and qualities while the inlet enthalpy remained constant ($\Delta h_c \text{ max.} = \pm 23 \text{ kJ/kg}$). The graphs include data for nozzles #2, #3, and #4 since the experimental error involved made it impossible to distinguish differences between the data of the three nozzles as initially expected. As calculational techniques became available this fact was reconfirmed as the expected differences between the thrust coefficients for the three nozzles were calculated to be small and not measureable within the accuracy of the experiments.

By comparing Figures 9 and 10 it is seen that the nozzles appear to operate more efficiently at higher plenum chamber pressures for equal back pressures. Probably the greatest factors influencing this phenomenon are the complex shock wave processes occurring for the different operating conditions. However, all the curves approach the optimum operating conditions with thrust coefficients in the 92-94 percent range. As previously indicated, this represents nozzle efficiencies in the range of 85 to 88 percent. Also, it should be noted that the peak thrust location corresponds to the operating condition where no shock or expansion waves are present. This can be seen by comparing Figures 7 and 10.

For comparison, Figures 9 and 10 also contain the optimum thrust coefficient as predicted by the numerical computer program of Elliott and Weinberg (1968). The magnitudes of the predicted thrust coefficients are somewhat lower than the experimental optimum values. This indicates that the losses involved within the nozzle flow are not as substantial as the numerical program predicts.

Tests are continuing with the low pressure nozzle #7 for which thrust coefficient data will not be presented. However, preliminary results indicate
optimum thrust coefficients in the 92-96 percent range. Shock wave effects in the two phase flow are also being studied both theoretically and experimentally. Also, since rectangular geometry is more compatible with the turbine wheel, work is continuing on the resulting two and three-dimensional effects upon the nozzle expansion process.

CONCLUSIONS

1. Thrust coefficients for the optimum nozzle back pressures were determined to be in the 92-94 percent range, which indicates efficiencies in the range of 85 to 88 percent for the nozzles and conditions presented. However, refinements in nozzle design may lead to even higher efficiencies.

2. Two-phase shock and expansion waves occur in the nozzles when operating at non-optimum back pressure conditions. A two-phase shock wave is much thicker than a perfect gas shock wave and does not seem to be as abrupt due to the interphase transfer mechanisms present, i.e., mass, momentum, and energy transfer between phases.

3. The simple IHE theoretical model is a simple but reasonably accurate tool for design and performance predictions. To gain a more complete solution technique, a more complex model such as the computer program of Elliott and Weinberg (1968) must be used.

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NOMENCLATURE

\begin{align*}
A &= \text{Flow Area} \\
C_T &= \text{Thrust or Velocity Coefficient} \\
F &= \text{Nozzle Thrust} \\
h &= \text{Specific Enthalpy} \\
\dot{m} &= \text{Mass Flow Rate} \\
p &= \text{Pressure} \\
r &= \text{Vapor Mass Flow Rate Ratio, } \dot{m}_v/\dot{m}_t \\
s &= \text{Specific Entropy} \\
T &= \text{Temperature} \\
V &= \text{Velocity} \\
x &= \text{Quality, } m_v/m_t \\
\beta &= \text{Slip Ratio, } V_{el}/V_{ev} \\
\eta^2 &= \text{Efficiency Coefficient} \\
\rho &= \text{Density} \\
\end{align*}

Subscripts

\begin{align*}
b &= \text{Back Pressure} \\
c &= \text{Plenum Chamber Condition} \\
e &= \text{Exit Plane Condition} \\
\lambda &= \text{Liquid Phase} \\
s &= \text{Isentropic Condition} \\
t &= \text{Total or Combined Phases} \\
v &= \text{Vapor Phase} \\
\end{align*}

Superscripts

\begin{align*}
\ast &= \text{Nozzle Throat Condition} \\
\end{align*}

REFERENCES


LIST OF FIGURES

Figure 1 - A photograph of a typical nozzle: (a) With a rubber injection molding of interanal contour alongside. (b) Installed inside nozzle test chamber prior to operation.

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Figure 3 - General nozzle test station and operation cycle schematic.

Figure 4 - Visual observations of the nozzle exit jet-flow pattern: (a) Overexpanded. (b) Near optimum. (c) Underexpanded.

Figure 5 - Experimental pressure profile comparison between the results of Starkman (1964) and those of nozzle #2.

Figure 6 - Experimental pressure profiles for nozzle #2 at constant inlet enthalpy and varying inlet pressures ($P_b = 100.7$ kPa).

Figure 7 - Experimental and theoretical pressure profiles for nozzle #2 at constant inlet conditions and varying back pressures.

Figure 8 - Experimental and theoretical pressure profiles for nozzle #7 at constant inlet conditions and varying back pressures.

Figure 9 - Thrust coefficient data as a function of back pressure and nozzle inlet conditions for nozzles #2, #3, and #4.

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