Development of a liquefied-gas micro-satellite propulsion system

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Abstract

The development of a liquefied-gas micro-satellite propulsion system using butane as propellant is presented. Such thrusters would be used to provide secondary propulsion to a micro-satellite in orbit. A theoretical model consisting of four control volumes was developed. For each control volume the principles of continuity and energy were applied to obtain the governing equations. Classical, ideal gas dynamic theory was used to solve for flow through the nozzle. Using this model computer simulations were run in order to observe theoretical thruster performance. Theoretical results indicated steady thrusts of 43 mN for a starting pressure of 300 kPa.

Experimental laboratory work was also done. This involved the design, manufacture and testing of a prototype system. Peak thrusts of approximately 50 mN were measured for testing in atmospheric conditions with a starting pressure of between 270 and 290 kPa. An equation to correlate the experimental data was developed. This correlated most of the experimental data to within ± 25 %.

Nomenclature

- $A$ : area, m$^2$
- $c_p$ : specific heat at constant pressure, J/kg·K
- $F_T$ : thrust, N
- $g$ : gravitational acceleration, m/s$^2$
- $h$ : enthalpy, J/kg·K
- $I_{sp}$ : specific impulse, s
- $K$ : phase change constant for fluid
- $m$ : mass, kg
- $m_\text{f}$ : mass flow rate, kg/s
- $u$ : velocity, m/s
- $p$ : pressure, Pa
- $Q$ : heat transfer rate, W
- $R$ : specific gas constant, J/kg·K
- $t$ : time, s
- $T$ : temperature, K
- $y$ : dependent variable
- $x$ : independent variable

Greek symbols

- $\sigma$ : condensation coefficient

Superscripts

- $a$ : correlation constant
- $b$ : correlation constant
- $c$ : correlation constant

Subscripts

- $e$ : exit
- $B$ : back
- $g$ : gas
- $lv$ : liquid-vapour
- $sat$ : saturated

1 Introduction

Secondary propulsion systems on satellites refer to propulsion applied to the satellite whilst in its orbit. Such systems can be used on micro-satellites for orbit lifting, speed adjusting, gravitation compensation, station-keeping and attitude control (Zakirov et al. 2001). Until recently small low-earth orbit (LEO), low-cost micro-satellites have flown without propulsion systems. This trend however, is expected to change and most future small spacecraft will contain propulsion systems (Gibbon and Underwood, 2001). If reliable, accurate systems can be developed at a low cost, proposed concepts of satellite clusters will become more feasible.

Traditionally, secondary propulsion requirements for LEO satellites have been fulfilled by cold gas systems in which the propellant is typically nitrogen stored at a very high pressure (in the order of 200 MPa) or hydrazine. The disadvantage of these high pressure systems are that they require extensive safety testing and documentation due to the dangers associated with storing a gas at very high pressure. Recently, attention has been given to the development of liquefied-gas thrusters (Gibbon and Underwood, 2001, Mukerjee et al., 2000 and Ye et al., 2001) where the propellant is stored in liquid phase, vaporised and expelled as a gas. The advantage of these systems is that the pressures at which the propellant is stored at is significantly lower than those of cold gas systems. In addition because the propellant is stored as a liquid, such systems require less volume – a significant advantage as micro-satellites are more often constrained by volume than by mass.
There are a variety of other propulsion systems that have recently been investigated. Thrusters using microelectromechanical systems (MEMS) technology have been considered by Ye et al. (2001) and Mukerjee et al. (2000). Ziemen et al. (1997) focus on the development of a pulsed plasma thruster. Toyoda et al. (2001) report on the development of a continuous carbon dioxide laser thruster. However, since it was desired to develop a simple, low-cost propulsion system these systems are regarded as too costly and complex for this application.

Researchers at the University of Surrey have performed work on butane thruster systems. Gibbon and Underwood (2001) discuss the development of a butane system that was successfully used on board the nano-satellite SNAP-1. Using 32.6 grams of butane the thruster system was able to raise the spacecraft’s orbit by over 3 km, which related to an effective \( \Delta V \) of between 1.9 and 2.1 \( \text{m/s} \). The mission specific impulse and effective thrust were calculated to be 43 s and 46 mN respectively. Gibbon and Underwood (2001) also put forward recommendations for building a butane propulsion system that could be used onboard a micro-satellite. These include using a conventional tank in which to store the butane (as opposed to the coiled tube arrangement used on SNAP-1) and to perform heating of the fuel in a separate thrust chamber assembly rather than just heating the nozzle.

In this paper work done towards the development of a liquefied-gas micro-satellite propulsion system using butane as propellant is presented. The work consisted, firstly, with the establishment of a theoretical model for the specific thrust as a function of the numerous design variables and, secondly the verification thereof using an experimental model.

2 Theoretical Modelling

2.1 Control Volume Approach

A one-dimensional explicit transient mathematical model was developed to simulate the operation of the two-phase propulsion system. The system was modelled as four separate control volumes, liquid (in storage tank), vapour (in contact with liquid in storage tank), accumulator (where vapour can be superheated) and valve-chamber (the volume between the 2nd solenoid valve and the nozzle throat). A schematic diagram of the system is shown in Figure 1. Between the storage tank and the accumulator, and between the accumulator and the valve chamber are solenoid valves which when open can be modelled by an equivalent length and diameter of pipe. It can be shown (White, 1999), using a one-dimensional steady-state momentum balance, that the resultant thrust produced by a rocket (depicted in Figure 2) is

\[
F_T = m_e u_e + A_e (p_e - p_B)
\]

Since \( A_e \) and \( p_B \) are generally known, it is necessary to calculate the mass flow rate \( m_e \), exit velocity \( u_e \) and exit pressure \( p_e \) in order to determine the thrust \( F_T \). Classical gas dynamic theory, which assumes an ideal, calorically perfect gas, is used to model the behaviour of the butane gas through the nozzle, as it was found that in the regions worked in, both the assumptions of ideal and calorically perfect gas were reasonable. The equations used for calculating \( m_e, u_e \) and \( p_e \) given the stagnation conditions and nozzle geometry have been dealt with in many texts – for example Anderson (1990) and White (1999). It was desired to model the nozzle as if it were exhausting to a total vacuum (as would be the case in space) then the flow through the nozzle would be totally shock-free, and the supersonic design solution would be applicable. However, in this case it was necessary to model the case of the nozzle exhausting to a finite back pressure, albeit low, in a vacuum chamber. In this situation the possibility of shock waves occurring could not be overlooked and had to be accounted for in the mathematical modelling of the system.

A detailed discussion of the theoretical modelling can be found in Weyer (2003). The only details regarding mathematical modelling that will be presented in this paper is the modelling of the phase change of the butane occurring between the liquid and vapour control volumes shown in Figure 1. The mass transfer from the liquid to the vapour control volume is due to phase change. This process was modelled as a non-equilibrium process in which superheated vapour and sub-cooled liquid could co-exist at different temperatures but without heat transfer taking place across the interface area \( A_{iv} \). (The heat of vapourisation \( Q_v \) in Figure 1 being supplied by the electric heater. The evaporation process is modelled by equation (2) (Mills, 1995). Employing the numbering for control volumes indicated in Figure 1 the equation is written as

\[
\dot{m} = A_{iv} K \left[ \frac{P_{sat} @ T_1}{\sqrt{T_1}} - \frac{P_2}{\sqrt{T_2}} \right]
\]

where \( K = 2\sigma ((2 - \sigma) / \sqrt{2\pi R}) \) is a constant for a given fluid with \( \sigma \) being the condensation coefficient (assumed to be one) and \( R \) is the ideal gas constant. \( A_{iv} \) is the surface area of the liquid-vapour contact area.
2.2 Numerical Procedure

In conducting the simulations it was necessary to use very small time steps (typically in the order of 0.0001 seconds) to avoid stability problems. The stability problems can be attributed to the fact that the control volumes worked with were very small, and if the time steps used were too large the mass flow out of a control volume over the length of a time step could be greater than the mass of the control volume, leading to negative mass.

The possibility of normal shock waves occurring in the divergent portion of the nozzle added an additional complexity to the simulation program. Even using classical dynamic theory it is not possible to explicitly calculate the position at which the shock occurs. An iterative procedure making use of interval halving was used. It was found that in the cases of normal shocks occurring in the divergent portion of the nozzle, the values of $m_e$, $u_e$ and $p_e$ should be calculated at the position just after the normal shock.

The properties used for butane in numerical simulations were those of normal butane (n-butane). However, for the experimental work butane used for refilling cigarette lighters was used. This butane is a mixture of normal butane, iso-butane and propane. The mixture ratio given by the manufacturer is: 54 % normal butane, 24% iso-butane and 22 % propane. Hence, the properties used in the simulation would not be truly accurate, since the butane was assumed to be 100 % pure normal butane.

With the assumption of 100 % pure normal butane there are still difficulties in determining the butane properties due to the fact that, aside from saturation properties, properties of a pure substance are a function of two variables, e.g. $h = f(p,T)$. Initially it was attempted to fit surfaces to the required properties in order to generate interpolation functions to give properties as functions of pressure and temperature for various pressure ranges using data for superheated butane. Although these interpolation functions were in general very accurate (typically within 2 %), due to the fact that different functions were used for different pressure ranges, there were slight...
discontinuities at the extreme points of the ranges. These discontinuities caused simulation programs to run into infinite loops when iterating near an extreme point of a range, since satisfactory convergence could not be obtained because the required property would be determined using a different interpolation function every iteration. In order to solve this problem a simple but less accurate equation was used to estimate the enthalpy of superheated butane in which the specific heat at constant pressure $c_p$ was approximated by averaging its values at saturated pressure and temperature. Hence, the equation used to determine the enthalpy $h$ of superheated butane vapour was:

$$h = h_{T_{sat}\text{Psat}} + c_p \left( T - T_{sat\text{Psat}} \right)$$

where $c_p$ is calculated as follows:

$$c_p = 0.5 \left( c_{p,sat\text{Psat}} + c_{p,T_{sat}} \right)$$

This method did not introduce discontinuities during iterations in the computer program. However, the results provided by this method were not as accurate as those obtained by surface fitting.

3 Experimental Model

The experimental model was designed, manufactured and tested in order to verify thruster performance. This would make it possible to compare theoretical predictions with experimental results. The experimental model incorporated locally available components that were relatively cheap (when compared to space-qualified components). The focus of the experimental testing was to prove the concept and verify theoretical results, rather than to qualify the design of the thruster system. Tests were carried out under both ambient and near-vacuum conditions in a vacuum chamber. Most of the model was constructed from perspex to make it possible to observe the propellant behaviour inside the tank and tubing. From a manufacturing point of view perspex is a difficult material to work with as it is brittle and has low strength (yield strength typically 50 MPa). Frequent breaking of components occurred during machining and handling. In addition special teflon ferrules had to be manufactured for use on the perspex tubing when connecting it to brass pipe connector fittings. This was because the standard brass ferrules supplied with the fittings could not be used as they would have cracked the perspex. It was also noted there was an adverse reaction of the perspex to the butane. After a few weeks of testing many fine cracks and stress lines could be seen on parts on the perspex, particularly in the storage tank. However, the damage was only on the surface and did not directly result in any material failures occurring.

The assembled model was mounted to a perspex base plate that was bolted horizontally onto the mounting block inside the vacuum chamber. A schematic diagram of the experimental model is shown in Figure 3, an isometric assembly drawing can be seen in Figure 4 and a photographic image is given in Figure 5.

![Figure 3 Schematic diagram of experimental model](image-url)
Figure 4 Isometric assembly drawing of experimental model

Figure 5 Labelled photograph of experimental model
4 Results

4.1 Theoretical Results

Theoretical results were obtained from a computer program incorporating the mathematical modelling discussed previously. A base case is defined in Table 1 and the results are presented graphically in Figure 6 which depicts a thrust and pressure curve for the model shown in Figure 3. For this simulation the storage tank valve in Figure 3 was held open and the nozzle valve was opened for two seconds.

Table 1 Base case parameters

<table>
<thead>
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<th>Parameter</th>
<th>Value</th>
<th>Units</th>
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<tbody>
<tr>
<td>Starting pressure</td>
<td>300</td>
<td>kPa</td>
</tr>
<tr>
<td>Starting temperature</td>
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<td>K</td>
</tr>
<tr>
<td>Back pressure</td>
<td>100</td>
<td>kPa</td>
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<td>Nozzle throat diameter</td>
<td>1</td>
<td>mm</td>
</tr>
<tr>
<td>Nozzle exit diameter</td>
<td>5</td>
<td>mm</td>
</tr>
<tr>
<td>Storage valve orifice diameter</td>
<td>2.8</td>
<td>mm</td>
</tr>
<tr>
<td>Nozzle valve orifice diameter</td>
<td>1.6</td>
<td>mm</td>
</tr>
<tr>
<td>Storage tank volume</td>
<td>251 x 10^-6</td>
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</tr>
<tr>
<td>Accumulator volume</td>
<td>31.4 x 10^-6</td>
<td>m^3</td>
</tr>
<tr>
<td>Valve chamber volume</td>
<td>2.36 x 10^-6</td>
<td>m^3</td>
</tr>
<tr>
<td>Ideal gas constant</td>
<td>138</td>
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</table>

Simulations were run to investigate the effect of changes in various parameters on the thrust produced, i.e. all the parameters of the base case given in Table 1 were held constant while only one of the parameters was varied at a time. Figure 7(a) shows peak thrust as a function of the initial pressure in the storage tank and accumulator. The peak thrust behaves as a fairly linear function of the initial pressure. The theoretical effect of back pressure on peak thrust is depicted in Figure 7(b). It can be seen that the peak thrust actually decreases slightly with a decrease in the back pressure. Below a back pressure of approximately 15 kPa the peak thrust suddenly increased dramatically. This sudden increase in peak thrust for low back pressure can be attributed to an absence of shocks in the nozzle.

The thrust curve in Figure 6(a) depicts a sudden rise in the thrust to a peak of approximately 0.06 N, followed by a tapering off to a steady thrust of 0.043 N that is maintained for the duration of the firing. This constant level of thrust will be referred to as the steady thrust and should be distinguished from the peak thrust (0.06 N in Figure 6(a)). It can be seen that the pressure curve in Figure 6(b) follows a similar trend to the corresponding thrust curve. There is an initial drop in pressure following the opening of the valve that is followed by a period of constant pressure. After closing of the nozzle valve the pressure increases to the initial pressure level, i.e. pressure is regained after a firing. This is due to the fact that when the butane pressure drops, the temperature of the liquid butane remaining is higher than the saturation temperature of the new pressure. Hence, liquid butane boils off, causing the pressure to increase until it reaches the saturation pressure corresponding to the temperature of the system. This is what Zakirov et al. (2001) refer to as the self-pressurising effect of liquefied-gas systems.

Effects due to nozzle variations can be seen in Figure 8 where steady thrust as a function of throat diameter and exit diameter is depicted. It is seen that there is a non-linear increase in thrust with an increase in throat diameter in Figure 8(a). This can be attributed to the fact that a bigger nozzle allows for a larger mass flow rate and thus a bigger thrust. From Figure 8(b) it is seen that the thrust is insensitive to increases in the exit diameter above about 2 mm. However, there is a dramatic increase in thrust for decreases in exit diameter less than about 1.5 mm – i.e. for nozzles with an exit diameter just larger than the throat diameter of 1 mm.
4.2 Experimental Results

Experimental data for butane exhausting to a 100 kPa back pressure is depicted in Figure 9 and Figure 10. The physical parameters of the experimental model are given as the base case values listed in Table 1. The data for Figure 9 was obtained by removing the nozzle valve and operating the storage tank valve, whereas the data in Figure 10 was obtained by keeping the storage tank valve open and operating the nozzle valve.

It can be seen from Figure 9(a) and Figure 10(a) that whilst similar levels of thrust are obtained, the shape of the thrust curves are different. The solenoid valves operated to generate the thrusts (i.e. the storage tank valve for Figure 9, and the nozzle valve for Figure 10) were both opened at time 1 s and closed at time 3 s. The thrust curves in Figure 10(a) are much steeper at these times (especially when the valves were closed). In Figure 9(a) there was thrust for about 0.5 s after the valve was closed. This is due to the fact that there was some liquid in the accumulator control volume and even after the valve was closed the liquid continued to evaporate providing a supply of gas at a higher pressure than the back pressure.

It is also interesting to examine the pressure graphs (Figure 9(b) (storage tank pressure) and Figure 10(b)). The most important aspect to note is that the pressure recovers after it has dropped due to a firing of the thruster. This is due to the fact that when the butane pressure drops, the temperature of the liquid butane remaining is higher than the saturation temperature at the new pressure. Hence, liquid butane boils off, causing the pressure to increase until it reaches the saturation pressure corresponding to the temperature of the system. This self-pressurising effect was captured by the theoretical model (see section 4.1)
The specific impulse of the butane in the system was estimated experimentally by conducting a test generating a 5 s pulsed thrust as shown in Figure 11. The difference in mass of the liquid butane before and after the pulse was estimated, by observing the drop in the level of the liquid, to be 0.43 grams. The equation used for specific impulse is as follows:

$$I_{sp} = \frac{\Delta m}{\Delta t} - \frac{\int_0^t F_t \, dt}{\Delta t} \cdot g$$  \hspace{1cm} (5)

Using equation (5) the specific impulse is calculated as

$$I_{sp} = \frac{0.03}{430 \times 10^{-6}} = 36 \text{ s}$$  \hspace{1cm} (6)

The specific impulse $I_{sp}$ as calculated using equation (5) gives a relative measure of how well the energy in the butane is being converted into thrust. The experimental value reported by Gibbon and Underwood (2001) was 43 s, about 20% higher than the value of 36 determined for the curve in Figure 11.

Figure 12 shows comparisons between theoretically predicted and experimentally measured results. It can be seen in Figure 12(a) that the theoretical model tends to slightly over-predict the measured thrusts. It can also be seen that the discrepancy between theoretical and experimental results increases at lower starting pressures. A correlation between theory and experiment is also shown in Figure 12(b), with the largest discrepancy occurring at the lowest back pressure. In general, judging from the results in Figure 12, it can be seen that the theoretical model was in fact reasonably accurate in predicting thrusts.
4.3 Correlation for Experimental Data

In order to generate a correlation between initial pressure and duration of thrust, data from a number of experiments was used to generate a function describing the average thrust as a function of starting pressure and duration of thrust. It is important to note that for this purpose the thrust value used was the average thrust, defined as

\[ \bar{F}_T = \frac{\int F_T \, dt}{\Delta t} \]  

(7)

It was assumed that the expression correlating average thrust in terms of pulse duration and pressure could be given by

\[ y = ax_1^b x_2^c \]  

(8)

where \( y \) is the dependant variable (average thrust) which is a function of two independent variables \( x_1 \) and \( x_2 \) (pulse duration and initial pressure), and \( a \), \( b \) and \( c \) are the constants. These constants are determined by performing a multi-linear regression analysis. The coefficient of determination \( R^2 \) indicating the accuracy of the fit was also calculated. The values for \( a \), \( b \), \( c \) and \( R^2 \) are given in Table 2. Examination of this table reveals that the value of \( b \) is much smaller than that of \( c \) (about 200 times) and is very close to zero. Hence, it is clear that \( x_2 \) (initial pressure) is the dominant variable in the correlation. In order to verify this, two further multi-linear regression analyses were performed assuming average thrust to be a function of pulse duration only (\( c = 0 \)) and of initial pressure alone (\( b = 0 \)). These results can also be found in Table 2. It can be seen that a low \( R^2 \) value of 0.1417 was obtained when average thrust is correlated as a function of pulse duration; thus indicating a poor correlation. However, the results for the correlation using initial pressure as the only independent variable are very similar to that obtained using the two independent variables. Hence, the following equation can be used to predict average thrust \( \bar{F}_T \) in N as a function of the initial pressure \( p \) in kPa:

\[ \bar{F}_T = 13.706 \times 10^{-6} p^{1.36229} \]  

(9)

Using the correlation given in equation (9) Figure 13 and Figure 14 are given to present graphically the accuracy with which equation (9) correlates the experimental data from which it was obtained. In Figure 13 it can be seen that equation (9) correlates the data reasonably well. There is one stray experimental data point for a 0.06 N average thrust. However, this was for the highest experimental average thrust used and is thus at the limit of the range. The comparison between the experimental data and the correlation against initial pressure shown in Figure 14 indicates a fair amount of scattering of the experimental data points about the correlation curve.

| Table 2 Values for correlation as function of pulse duration and initial pressure |
|-----------------|-------|-------|-------|
| \( \bar{F}_T \) | \( a \) | \( b \) | \( c \) | \( R^2 \) |
| \( F_T = a t^b p^c \) | 12.87 \times 10^{-6} | 0.007027 | 1.3720 | 0.5636 |
| \( F_T = a t^b p^0 \) | 0.03525 | -0.25313 | 0 | 0.1417 |
| \( F_T = a t^0 p^c \) | 13.71 \times 10^{-6} | 0 | 1.3623 | 0.5636 |

Figure 13 Graphical comparison between experimental data and correlation

Figure 14 Experimental and correlated average thrust against starting pressure
5 Conclusions

The theoretical and experimental work done for the development of a liquefied-gas butane propulsion system has been documented. The modelling of the phase change behaviour of the butane in the storage tank required an awkward assumption, namely that of non-thermodynamic equilibrium between the liquid and vapour butane in the storage tank in order to use equation (2) to describe the mass transfer between liquid and vapour. In addition this equation required knowledge of the condensation coefficient $\sigma$ whose value is a source of concern, even for a common fluid such as water. Another uncertainty in the simulation was the value used for the liquid-vapour interface $A_{lv}$ in equation (2). The value used was the cross-sectional area of the cylindrical storage tank. However, in the experimental work the storage tank was placed horizontally and hence the surface area of liquid in contact with the vapour would have been different from the cross-sectional area used in the simulations. The incorrect value of $K$ that was used in equation (2) may have corrected the wrong value of the liquid-vapour interface area that was used in equation (2). It is recommended that more attention could be given in the future to the numerical simulation of the thermo-fluid behaviour of the propellant and attention in particular given to the following aspects: modelling of the two-phase behaviour inside the storage tank, development of a procedure to model the mass transfer between liquid and vapour under changing pressure environments and subdivision of the main control volumes into smaller control volumes, i.e. making use of a finer grid. The use of commercially available computational fluid dynamics (CFD) software could also be considered as it would make more detailed analyses possible.

It can be seen that in Figure 8 that reducing the nozzle exit to throat ratio (i.e. by increasing the throat diameter as in Figure 8(a), or by reducing the exit diameter as in Figure 8(b)), increases the thrust when exhausting to a back pressure of 100 kPa. This is due to the fact that a lower exit to throat ratio does not require as low a back pressure to achieve supersonic shock-free flow in the diverging portion of the nozzle.

Notwithstanding the above potential discrepancies in the theoretical model, it is shown that the theoretical model may be used with reasonable confidence in the engineering design of a thruster system.

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