Applications of Fibre Optics in Solar Thermal Propulsion Systems

P.R. Henshall

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Unis

Surrey Space Centre
School of Electronics and Physical Sciences
University of Surrey
Guildford, Surrey GU2 5XH, UK

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Abstract

Solar thermal propulsion (STP) is the utilisation of concentrated sunlight for heating a propellant to high temperatures. Early STP concepts were envisioned for large spacecraft and capable of high levels of propulsive performance (<1000s Isp). Currently, at the University of Surrey, STP is being adapted for use on small-spacecraft in order to utilise the high propulsive capability offered by STP and widen the range of small-spacecraft applications. Conventional STP system concepts encounter difficulties in conforming to the low mass and volume requirements of a small-spacecraft platform. An enabling technology for the miniaturisation of an STP system is seen in the advent of low attenuation high numerical aperture (NA) fibre optics. This work investigates the mission and design implications of an STP system augmented with fibre optics and develops new technologies that stem from the concept.

A small parabolic dish concentrator was designed to the requirements of a high NA optical fibre and manufactured for component testing. Efficiency tests of the small concentrator demonstrated 83% efficiency and an overall system efficiency of 50% including coupling light into the fibre.

A fibre optic heat exchanger was designed, manufactured and tested to investigate methods of improving heat transfer efficiency. Tests of the heat exchanger demonstrated receiver absorption efficiencies of 82%.

Stringent solar pointing accuracies imposed by the small concentrator-fibre optic combination resulted in the development and testing of a novel sensor technology that employs fibre optic luminescence as feedback for a concentrator pointing control mechanism. Concentrator pointing accuracies of 3 arc-minutes were experimentally demonstrated.

Accompanying this work is the development of a novel algorithm for the study of coupled radiation and conduction heat transfer within participating media, which is more accurate and stable than conventional techniques.

This work successfully demonstrated the potential high efficiency and feasibility of a small-spacecraft fibre augmented STP system.
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<td>One-Dimensional</td>
</tr>
<tr>
<td>2D</td>
<td>Two-Dimensional</td>
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<tr>
<td>ABCD</td>
<td>Referring to the ABCD pointing algorithm</td>
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<td>ATI</td>
<td>Advanced Technology Institute</td>
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<tr>
<td>AETB-12</td>
<td>Ceramic Insulation</td>
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<td>AFRL</td>
<td>U.S. Air Force Research Laboratory</td>
</tr>
<tr>
<td>AFRPL</td>
<td>U.S Air Force Rocket Propulsion Laboratory</td>
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<td>AM0</td>
<td>Air Mass Zero</td>
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<td>ALSAT-1</td>
<td>Algerian contribution to the DMC micro-satellite constellation</td>
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<td>NH₃</td>
<td>Ammonia</td>
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<td>AU</td>
<td>Astronomical Unit</td>
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<td>Turkish contribution to the DMC micro-satellite constellation</td>
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<td>BK7</td>
<td>Optical Glass Substrate</td>
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<tr>
<td>CVD</td>
<td>Chemical Vapour Deposited</td>
</tr>
<tr>
<td>CNES</td>
<td>Centre National d’Études Spatiales</td>
</tr>
<tr>
<td>CTE</td>
<td>Coefficient of Thermal Expansion</td>
</tr>
<tr>
<td>CSPFS</td>
<td>Cryogenic Storage and Propellant Feed System</td>
</tr>
<tr>
<td>DMC</td>
<td>Disaster Monitoring Constellation</td>
</tr>
<tr>
<td>EM</td>
<td>Electro-Magnetic</td>
</tr>
<tr>
<td>ESA</td>
<td>European Space Agency</td>
</tr>
<tr>
<td>EXCEL</td>
<td>Microsoft data base software</td>
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<tr>
<td>FT1EMT</td>
<td>3M Ltd product name for a fibre optic cable</td>
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<tr>
<td>GEMINI</td>
<td>SSTL manufactured mini-satellite</td>
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<td>GEO</td>
<td>Geo-synchronous Orbit</td>
</tr>
<tr>
<td>GIOVE-A</td>
<td>SSTL manufactured satellite, first to broadcast Galileo GPS signals</td>
</tr>
<tr>
<td>GTO</td>
<td>Geo-synchronous Transfer Orbit</td>
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<td>HCN-N1000T-14</td>
<td>SpecTran Ltd product name for a fibre optic cable</td>
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<tr>
<td>HLEO</td>
<td>High Latitude Earth Observation</td>
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<tr>
<td>HOSS</td>
<td>Hydrogen On-Orbit Storage And Supply Experiment</td>
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<tr>
<td>N$_2$H$_4$</td>
<td>Hydrazine</td>
</tr>
<tr>
<td>Oxidiser, H$_2$O$_2$</td>
<td>Hydrogen Peroxide</td>
</tr>
<tr>
<td>O-H</td>
<td>Hydroxyl Ion</td>
</tr>
<tr>
<td>IR</td>
<td>Infrared</td>
</tr>
<tr>
<td>IAE</td>
<td>Inflatable Antenna Experiment</td>
</tr>
<tr>
<td>ISUS</td>
<td>Integrated Solar Upper Stage</td>
</tr>
<tr>
<td>Isp</td>
<td>Specific Impulse</td>
</tr>
<tr>
<td>L$_1$</td>
<td>Lagrange point</td>
</tr>
<tr>
<td>LO$_2$-LH$_2$</td>
<td>Liquid Oxygen and Liquid Hydrogen</td>
</tr>
<tr>
<td>LAD</td>
<td>Liquid Acquisition Device</td>
</tr>
<tr>
<td>LEO</td>
<td>Low Earth Orbit</td>
</tr>
<tr>
<td>LOT-Oriel</td>
<td>Optical component company</td>
</tr>
<tr>
<td>LVDT</td>
<td>Linear Voltage Displacement Transducer</td>
</tr>
<tr>
<td>MACOR</td>
<td>Insulation material</td>
</tr>
<tr>
<td>Matlab</td>
<td>Mathworks Ltd computational analysis software</td>
</tr>
<tr>
<td>MD22</td>
<td>Devantech Ltd product, motor drive amplifier circuit</td>
</tr>
<tr>
<td>MEO</td>
<td>Medium Earth Orbit</td>
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<tr>
<td>MidIR</td>
<td>Medium Infrared</td>
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<tr>
<td>MDI</td>
<td>The Michelson Doppler Imager</td>
</tr>
<tr>
<td>MICROSCOPE</td>
<td>CNES microsatellite</td>
</tr>
<tr>
<td>MEMS</td>
<td>Micro-Electro-Mechanical System</td>
</tr>
<tr>
<td>MMH</td>
<td>Mono-Methyl-Hydrazine</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
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<td>--------------</td>
<td>-------------</td>
</tr>
<tr>
<td>MYRIADE</td>
<td>CNES microsatellite program</td>
</tr>
<tr>
<td>NA</td>
<td>Numerical Aperture</td>
</tr>
<tr>
<td>NADIR</td>
<td>Referring to the facet of a spacecraft pointing towards the Earth</td>
</tr>
<tr>
<td>NAL</td>
<td>National Aerospace Laboratory</td>
</tr>
<tr>
<td>NASA</td>
<td>National Astronautical Space Administration</td>
</tr>
<tr>
<td>NASA-LeRC</td>
<td>National Lewis Research Centre</td>
</tr>
<tr>
<td>GRC</td>
<td>NASA's Glen Research Centre</td>
</tr>
<tr>
<td>NIR</td>
<td>Near Infrared</td>
</tr>
<tr>
<td>N₂O₄</td>
<td>Nitrogen tetroxide</td>
</tr>
<tr>
<td>NRIM</td>
<td>National Research Institute of Metals</td>
</tr>
<tr>
<td>OWSP</td>
<td>Optical Waveguide Solar Plant</td>
</tr>
<tr>
<td>Optran Ultra</td>
<td>CeramOptec Ltd product name for a fibre optic cable</td>
</tr>
<tr>
<td>Optran UVNS</td>
<td>CeramOptec Ltd product name for a fibre optic cable</td>
</tr>
<tr>
<td>OSA</td>
<td>Optical Spectrum Analyser</td>
</tr>
<tr>
<td>PICARD</td>
<td>CNES microsatellite</td>
</tr>
<tr>
<td>POE</td>
<td>Precision Optical Engineering Ltd</td>
</tr>
<tr>
<td>RAC</td>
<td>Receiver-Absorber-Converter</td>
</tr>
<tr>
<td>RAAN</td>
<td>Right Ascension of the Ascending Node</td>
</tr>
<tr>
<td>RMS</td>
<td>Root Mean Square</td>
</tr>
<tr>
<td>RT</td>
<td>Room Temperature</td>
</tr>
<tr>
<td>STK</td>
<td>Satellite Tool Kit</td>
</tr>
<tr>
<td>Simulink</td>
<td>Mathworks Ltd computational analysis software</td>
</tr>
<tr>
<td>SODISM</td>
<td>Solar Diameter Imager and Surface Mapper</td>
</tr>
<tr>
<td>SOHO</td>
<td>Solar and Heliospheric Observatory</td>
</tr>
<tr>
<td>SOTV</td>
<td>Solar Orbit Transfer Vehicle</td>
</tr>
<tr>
<td>SolidEdge</td>
<td>Computer Aided Design software package</td>
</tr>
<tr>
<td>SSTL</td>
<td>Surrey Satellite Technology Ltd</td>
</tr>
<tr>
<td>STP</td>
<td>Solar Thermal Propulsion</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
</tr>
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<td>-------------</td>
<td>-----------------------------------------------------------------------------</td>
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<tr>
<td>STU-D</td>
<td>Polymicro Ltd product name for a fibre optic cable</td>
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<tr>
<td>SFS-T</td>
<td>Fibreguide Ltd product name for a fibre optic cable</td>
</tr>
<tr>
<td>TIR</td>
<td>Total Internal Reflection</td>
</tr>
<tr>
<td>TVS</td>
<td>Thermodynamic Vent System</td>
</tr>
<tr>
<td>TiB2/BN</td>
<td>Titanium Boron Nitride</td>
</tr>
<tr>
<td>TW600/620</td>
<td>CeramOptec Ltd product name for a fibre optic cable</td>
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<tr>
<td>UARS</td>
<td>Upper Atmosphere Research Satellite</td>
</tr>
<tr>
<td>UK-DMC</td>
<td>United Kingdom contribution to the DMC constellation</td>
</tr>
<tr>
<td>ULE</td>
<td>Ultra Low Expansion</td>
</tr>
<tr>
<td>Vis</td>
<td>Visible</td>
</tr>
<tr>
<td>VPGS</td>
<td>Virtual Polar Geo-stationary Satellite</td>
</tr>
<tr>
<td>XPC-Target</td>
<td>Software for real time hardware implementation</td>
</tr>
<tr>
<td>ZEMAX</td>
<td>Professional optical design software package</td>
</tr>
<tr>
<td>ZERODUR</td>
<td>An ultra low expansion glass</td>
</tr>
<tr>
<td>ZSBN</td>
<td>Zirconia-Strengthened Boron Nitride</td>
</tr>
</tbody>
</table>
Nomenclature

\(A\)  
Area \((m^2)\)

\(\hat{A}\)  
Along track vector

\(A_a\)  
Aperture area

\(A_f\)  
Focal spot area

\(A_e\)  
Nozzle exit area

\(A^*\)  
Nozzle throat area

\(a\)  
Semimajor axis (in chapter 3) \((m)\)

\(a\)  
Angular acceleration (in chapter 7) \((\text{rads} \cdot \text{s}^{-2})\)

\(B\)  
Boundary

\(Bi\)  
Biot number

\(b\)  
Damping ratio \((N \cdot \text{m} \cdot \text{s})\)

\(b(s)\)  
Feedback signal

\(CR_g\)  
Geometric concentration ratio

\(C_p\)  
Specific heat capacity \((J \cdot \text{kg}^{-1} \cdot \text{K}^{-1})\)

\(C(s)\)  
Controller transfer function

\(CS(s)\)  
Complementary sensitivity function

\(c\)  
Speed of light \((2.9979 \times 10^8 \text{ m/s})\)

\(D\)  
Medium dimension

\(D_{sp}\)  
Density specific impulse

\(d\)  
Fraction of year (in chapter 3)

\(d\)  
Diameter/distance (in chapter 4) \((m)\)

\(d_c\)  
Concentrator diameter

\(d_f\)  
Fibre diameter

\(d(s)\)  
Disturbance signal

\(den\)  
Density \((\text{kg} \cdot \text{m}^{-3})\)

\(E\)  
Young’s modulus (in chapter 4)

\(E\)  
Exponential integral function (in chapter 5)
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>F</td>
<td>Focus</td>
</tr>
<tr>
<td>f</td>
<td>Focal length</td>
</tr>
<tr>
<td>f_f</td>
<td>Darcy-Weisbach friction factor</td>
</tr>
<tr>
<td>G</td>
<td>Gaussian distribution</td>
</tr>
<tr>
<td>g</td>
<td>Gravitational acceleration (9.81 m/s²)</td>
</tr>
<tr>
<td>h</td>
<td>Concentrator height</td>
</tr>
<tr>
<td>h_conv</td>
<td>Convective heat transfer coefficient</td>
</tr>
<tr>
<td>h_r</td>
<td>Radiative heat transfer coefficient</td>
</tr>
<tr>
<td>I</td>
<td>Impulse (in chapter 2 &amp; 6)</td>
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<tr>
<td>I</td>
<td>Armature current (in chapter 7) (A)</td>
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<tr>
<td>I_0</td>
<td>Observed intensity (in chapter 4) (W/m²)</td>
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<td>I_sp</td>
<td>Maximum intensity</td>
</tr>
<tr>
<td>i</td>
<td>Specific Impulse (s)</td>
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<tr>
<td>i</td>
<td>Directional intensity (W/m²-str⁻¹)</td>
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<tr>
<td>i</td>
<td>Orbital inclination angle</td>
</tr>
<tr>
<td>i</td>
<td>Ray incident vector</td>
</tr>
<tr>
<td>J</td>
<td>Moment of inertia (kg·m²·s⁻²)</td>
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<tr>
<td>K_p</td>
<td>Equilibrium constant</td>
</tr>
<tr>
<td>K(s)</td>
<td>Sensor transfer function</td>
</tr>
<tr>
<td>k</td>
<td>Thermal conductivity (W/m·K⁻¹)</td>
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<tr>
<td>k_b</td>
<td>Boltzmann constant (1.38x10⁻²³ m²·kgs⁻²·K⁻¹)</td>
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<tr>
<td>k_a</td>
<td>Armature constant (N·m·A⁻¹)</td>
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<tr>
<td>k_o</td>
<td>Motor constant (V/(rad·s)⁻¹)</td>
</tr>
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<td>Cylinder length (in chapter 5)</td>
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<td>L</td>
<td>Inductance (H)</td>
</tr>
<tr>
<td>L</td>
<td>Thickness</td>
</tr>
<tr>
<td>M</td>
<td>Mass (kg)</td>
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<td>Burnout mass</td>
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<tr>
<td>M_o</td>
<td>Starting mass</td>
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<td>M_p</td>
<td>Propellant mass</td>
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<td>Symbol</td>
<td>Definition</td>
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<tr>
<td>$M_r$</td>
<td>Receiver mass</td>
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<tr>
<td>$\dot{M}$</td>
<td>Molecular mass (kg·kg$^{-1}$ mol$^{-1}$)</td>
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<tr>
<td>$\dot{m}$</td>
<td>Mass flow rate (kg·s$^{-1}$)</td>
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<tr>
<td>$m$</td>
<td>Time parameter</td>
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<td>$N$</td>
<td>Radiation–conduction parameter</td>
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<td>Avogadro's number (6.022×10$^{23}$)</td>
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x  Coordinate
Y  Medium dimension
y  Coordinate
y(s)  Output signal (frequency domain)
y(t)  Output signal (time domain)
z  Coordinate
z  Optical axis

**Greek Symbols**

\( \alpha \)  Angle of Earth tilt with respect to the ecliptic
\( \chi \)  Constituent mole fractions
\( \Delta \)  Distance/Difference
\( \delta \)  Deflection (m)
\( \varepsilon \)  Emissivity
\( \gamma \)  Ratio of specific heats
\( \eta \)  Efficiency (conversion of electric to thermal energy)
\( \kappa \)  Absorbtivity (m\(^{-1}\))
\( \lambda, \mu \)  Eigen values
\( \mu \)  Viscosity (Pa-s)
\( \mu \)  Earths gravitational constant (6.67\times10^{-11} \text{ N-m}^2\text{-kg}^{-2})
\( \nu \)  Spectrally dependant
\( \pi \)  Pi (3.14159)
\( \theta \)  True Anomaly (in chapter 3)
\( \theta \)  Angle
\( \theta \)  Angular position (in chapter 7)
\( \theta_f \)  Angular form error
\( \theta_s \)  Solar half angle
\( \dot{\theta} \)  Angular rate
\( \rho \)  Reflection angle
\( \rho \)  Core density
### List of Figures

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Chapter 1

1 Introduction

1.1 Research Motivation

Solar thermal propulsion (STP) is the utilisation of concentrated sunlight for the purposes of heating a propellant to high temperatures via a heat exchanger. Sunlight concentration is achieved via an optical concentrating system, such as a series of lenses or mirrors. This concentrated sunlight impinges on a blackbody cavity receiver, which is subsequently heated to high temperatures. A propellant feed system causes a monopropellant to flow around the cavity receiver where heat is exchanged from the receiver to the propellant. The sunlight concentration system is orientated via a spacecraft mechanism or via the spacecraft attitude system to point at the Sun; control electronics and sensors are employed to actuate the mechanism and provide telemetry.

Solar Thermal Propulsion has a number of key benefits relating to spacecraft propulsion.

- Utilisation of a free energy source: In directly using solar energy, a power generation system on board a spacecraft is not required. All that is required is the means of capturing and concentrating incident solar energy.

- High energy conversion efficiency: STP has the potential of achieving solar energy to heat conversion efficiencies from 50% to 80% [1, 2004]. This should be compared to current spacecraft solar photovoltaic technology which converts solar energy to electrical energy, with theoretical energy conversion efficiencies only as high as ~26% [2, 1999]. This means that for a given solar energy collection area a STP system could deliver more power to a propellant than a solar electric propulsion system, hence achieving better propulsive performance.

- High efficiency monopropellant propulsion system: STP is capable of providing spacecraft propulsive performances exceeding chemical bi-propellant propulsion systems (>350s), employing only a monopropellant [1, 2004].
The motivation of this research is to investigate the feasibility of using a STP system on board a small-spacecraft for orbital manoeuvres. A small-spacecraft making use of the high propulsive performance offered by STP, could be considered for a wider variety of space mission applications. Small-spacecraft are generally regarded as a low-cost means of space access [2, 1999][3, 2000] and subsequently in the last several years there has been an increasing interest in using small-satellites to perform missions which, have generally been regarded as the territory of larger (500 kg or greater) spacecraft [4, 2000]. Such missions include major orbit transfers, to geo-synchronous Earth orbit (GEO) and the LaGrange points, Lunar orbit, asteroids, and the inner planets (e.g., Mars, Venus) [4, 2000]. These missions require spacecraft velocity change increments exceeding (ΔV) 1,000m/s [1, 2004][4, 2000].

Conventional small-satellite propulsion systems are typically low performance systems, mainly due to the low resource capacity of a small-satellite. For example UoSAT-12, launched in 2000, was the university of Surrey’s first small satellite to incorporate a propulsion system, which had a ΔV capability of 27m/s [3, 2000]. Another example is the UK-DMC micro-satellite launched in 2003, weighing only 88kg, with dimensions of 640x640x680mm. Its propulsion system, a butane based monopropellant resistojet, produced a specific impulse of 85s and was capable of a 20m/s ΔV [5, 2004]. On the 28th of December 2005 the Giove-A spacecraft was launched, which was an European Space Agency (ESA) funded test satellite for the GALILEO global positioning system. The Giove-A spacecraft weighing 600kg with stowed dimensions of 1,300x1,800x1,650mm, had a butane monopropellant propulsion system capable of 90m/s ΔV [6, 2006]. Part of the problem with augmenting a small-spacecraft with a high performance propulsion system is overcoming the oppressive constraints regarding the spacecrafts mass and volume. Due to the benefits of STP (mentioned above), STP offers a promising choice for a small-spacecraft high performance propulsion system.

1.2 Previous and Current Research

STP has been under development for over 40 years. The research originally undertaken was commonly a costly venture due to the requirement to attain a high system performance on the order of 1000s specific impulse. This invariably required the use of very large inflatable/deployable solar concentrators that could collect a massive amount of solar power (see figure (1.1)). This power was delivered to a cavity receiver heat exchanger capable of operating at temperatures as high as 3000 K. The propellant selected was hydrogen, which at such high temperatures would deliver the required performance. Subsequently this demanded the design of large-scale spacecraft, dominated by their propulsion systems.
From the year 2000 to the present, research at the University of Surrey in the UK has concentrated on adapting STP to small-spacecraft. The STP system concept designed at Surrey is significantly simpler than previous STP concepts, foregoing any mechanisms and making use of more storable propellants, such as ammonia. This was necessary to overcome small spacecraft constraints. The system employs a ‘thermal-storage’ cavity heat exchanger and uses the spacecraft’s attitude control system to point the concentrator. The concentrator itself is a small rigid concentrator encapsulating one facet of the spacecraft (Figure 1.2a). This STP system design had constraints in terms of manoeuvring and firing strategies, but the system is robust, cheap, reliable and very competitive in terms of performance and ΔV capability, with the possibility of achieving specific impulses between 200 and 400s. Such a technology could allow a small-spacecraft to perform a larger variety of space missions [1, 2004], thus reducing the cost of space access, which is the typical trend of small-satellite budgets.

Using fibre optics for the transmission of highly concentrated solar energy (Figure 1.3a) is a concept that has existed since the 1970s. Since then potential applications of the technology have
been proposed that include: solar energy collection devices, solar surgery and nanomaterials manufacture [9, 1999]. Developments in fibre optic manufacture have provided core glass materials of progressively better quality and therefore improved light transmission properties. Experiments of light transmission through fibre optics, at the Ben-Gurion University of the Negrev, have shown potential for transmission efficiencies as high as 80% [9, 1999] for high numerical aperture fused silica fibre optics (0.66), over fibre lengths of a few meters. More recently, an investigation conducted by Nakamura et al [10, 2005] concerning the harnessing of concentrated sunlight, via fibre optics for heating of a solar receiver, demonstrated fibre transmission efficiencies of ~70% over fibre lengths of 4m.

In 2004 the use of fibre optics in a small-spacecraft STP system was proposed [11, 2004][12, 2004]. With the application of fibre optics to a small-spacecraft STP system, a degree of flexibility with regard to system design and thrusting can be realised. For example with fibre optics the cavity receiver can be mechanically decoupled from the concentrator and can be placed anywhere on the spacecraft. The system mass can also be significantly reduced as employing fibre optics allows multiple smaller concentrators, which can be independently pointed on-sun, to replace a single, larger, fixed concentrator (Figure (1.2b)). Given that the mass of a (concentrator) mirror scales as the 4th power of the diameter, smaller mirrors are favoured. This flexibility comes at a cost however; the energy travelling along the fibre is attenuated and reduces the end-to-end efficiency of the system. Also there is the added complexity of separately pointing each mirror in an array.

Augmenting a STP system with fibre optics has the potential for providing greater STP system versatility and further overcoming small-spacecraft constraints. This research investigates the feasibility, benefits, new technology and potential performance of a fibre augmented solar thermal propulsion system for a small-spacecraft.

![Figure 1.3: (a) Fibre optic transmission test at SSTL’s solar simulator facility. [1] (b) Fibre optic on-Sun transmission test at the Surrey Space Centre.](image-url)
1.3 Research Objectives

At the start of this research project a series of research objectives were established to provide a clear path of research direction. These research objectives were:

- Identification of significant benefits brought to a small spacecraft STP system through the introduction of fibre optics in terms of:
  - Orbital manoeuvring strategies.
  - System versatility and integration.
  - System redundancy.

- Identification of small-spacecraft high ΔV mission concepts, which could gain or become feasible through the application of a solar thermal propulsion system augmented with fibre optics.

- Investigation of significant system components and how the integration of fibre optics affects the design considerations. System components include:
  - Solar concentrator
  - Fibre optics
  - Solar receiver
  - Solar concentrator pointing system

- Derive system component requirements and overall system efficiency for an example fibre augmented STP system, through system component modelling.

- Experimentally investigate feasibility of obtaining component requirements.

- Design, manufacture and test prototype system.

1.4 Scope of Research

The main theme of this research is the technological investigation and development of a fibre augmented solar thermal propulsion system suitable for a small-spacecraft. However, it should be noted that the research discussed here has other potential applications in space and terrestrially. The initial aim of this research was to identify the benefits of the introduction of fibre optics to a solar thermal propulsion system, suitable for a small-spacecraft. A number of appropriate small-spacecraft missions that could gain from this technology are considered to determine mission
specific requirements and to investigate orbital manoeuvre strategies for fibre augmented solar thermal propulsion systems in comparison to conventional solar thermal propulsion systems. System requirements derived from an analysis of a modelled fibre augmented STP small-spacecraft demonstrator system, are passed through to the preliminary design of the separate components that make up the propulsion system. Each component is designed to the Surrey Space Centre's philosophy of low cost access to space. This requires that the system and component features be kept small, simple and affordable. This reduces the number of technological unknowns and provides a more fully developed system. These component designs are subjected to computational modelling through the use of standard and validated codes in the areas of optical, thermal and control systems design, to anticipate expected performance.

The first system component investigated was the optical solar concentration device. The scope of research for this component was to establish which means of solar energy concentration was most appropriate for a small-spacecraft STP system and how its design requirements change for use with fibre optics. Furthermore, any advantages of a fibre coupled concentrator system over a conventional STP concentrator system were identified. To demonstrate these advantages detailed modelling of the concentrator along with the consideration of practical issues was necessary. With the selection of a fibre optic cable for this application a concentrator component was designed manufactured and tested with the fibre optic to establish the manufactured component efficiency.

The second system component investigated was the fibre optic cable. The scope of the research for this component was to identify the issues concerning the feasibility of using fibre optics in space for the transmission of concentrated solar energy. In particular this research focussed on causes of fibre optic attenuation. A range of potential fibre optic-cables were considered based on those appearing regularly in the literature for this application. On identifying a suitable fibre optic cable a study was conducted to ascertain this fibres thermal behaviour when transmitting high intensity sunlight. From this thermal study a novel thermal algorithm was developed which was suitable for solving transient coupled radiation and conduction heat transfer problems for participating media. Moreover, the fibre was subjected experimentally to transmission and thermal tests to establish the real fibre optics efficiency and thermal behaviour.

The third system component investigated was the solar thermal receiver. The scope of the research for this component was to establish the fibre augmented STP demonstrator system performance and also to consider fibre optic heating of a solar thermal receiver with attention to the thermal environment to which a fibre optic will be exposed. A fibre optic cable heating a solar receiver will be exposed to temperatures exceeding the manufactures specifications, which could result in significantly reduced transmission efficiency. Thus in order to investigate efficient end-to-end fibre optic energy delivery a fibre optic heat exchanger was designed and constructed which guided fibre optics to heat a small refractory metal (molybdenum) sample representative of
a real thruster. This investigation provided insight into improvement of solar energy to heat conversion efficiency and demonstrated potential for efficient and safe fibre optic heating.

The final system component investigated was the concentrator solar pointing system. The initial scope of the research for this component was to establish the concentrator pointing accuracies required for efficient coupling of solar radiation from the concentrator system to the fibre optics and to investigate how this could be achieved if a small-spacecraft was unable to provide the required solar pointing accuracy. This resulted in the development of a novel sensor that was capable of monitoring fibre optic luminescence and interpreting it in to focal image plane position for pointing control feedback. This sensor was at first modelled, then manufactured and tested demonstrating consistency between simulated and real tracking. The sensor was then employed to provide feedback-pointing control a two-axis concentrator pointing mechanism that was used to track a solar simulator. This sensor is also potentially capable of operating as an optical switch, allowing the destination of the transmitted solar energy to be chosen. This opens up a whole range of STP configuration possibilities, which can be tailored to a specific mission. These new STP configurations were categorised.

1.5 Thesis Structure

This thesis describes an investigation of the application of fibre optics in solar thermal propulsion systems and develops relevant technologies to enable a flight demonstration. The current chapter provides a basic review of research activities in the areas of solar thermal propulsion and fibre optic transmission of high intensity sunlight. Discussed also is how this research benefits the state of the art and the scope of the research activity.

- Chapter 2, Literature Survey, reviews the history of solar thermal propulsion and considers each subsystem/component separately. Other areas of investigation include fibre optics for transmission of solar energy, methods of coupled radiative and conductive heat transfer and existing solar pointing control techniques.

- Chapter 3, Mission Analysis and STP System Variants, identifies two classes of STP propulsion system and discusses micro-satellite missions applicable to both. The benefits of augmenting both types of STP systems with fibre optics are also discussed. Two example spacecraft missions are considered in which it is shown that a fibre augmented solar thermal propulsion system is an enabling technology for small satellites to fulfil the propulsive mission objectives. A case study is performed to demonstrate the feasibility of employing an STP system on a micro-satellite. Finally a number of missions suitable for a fibre augmented STP technology demonstration are identified.
• Chapter 4, \textit{STP Concentrator Investigation}, discusses the investigation of appropriate solar concentrators for a small-satellite fibre augmented STP technology demonstration system. Initially, concentrator optics and concentrator-to-fibre optic coupling theory are reviewed. Then the benefits of fibre optic concentrator fibre optic coupling are addressed and design parameters are identified. Having identified the key design parameters for a fibre optic concentrator more detailed modelling of the concentrator is performed to identify required component properties for manufacture. Finally a series of tests are described concerning the efficiency and confirmation of the manufactured component properties.

• Chapter 5, \textit{Fibre Optics for STP}, discusses the investigation of appropriate fibre optics for a small-satellite fibre augmented STP technology demonstration system. Initially, the theory and properties of fibre optics are reviewed and a range of commercially available fibre optics is identified. With the selection of an appropriate fibre optic cable an investigation of the thermal behaviour of the fibre optic when transmitting concentrated solar energy and when exposed to high temperature is conducted. Resulting from this study is the development of a novel algorithm for coupled conduction and radiation heat transfer, which is described here. Finally a series of fibre optic tests are conducted, including: power transmission efficiency tests and thermal tests.

• Chapter 6, \textit{STP Receiver Investigation}, discusses the investigation of appropriate solar receivers for a small-satellite fibre augmented STP technology demonstration system. Initially, the theory of propulsion is reviewed and the properties of direct-gain and thermal-storage solar receivers are identified. An ammonia direct-gain receiver modelling tool, created for this research, is subsequently discussed and used to determine the performance of a fibre optic augmented STP demonstration system for a small-satellite. Finally a practical investigation of fibre optic heating via a fibre optic heat exchanger is discussed.

• Chapter 7, \textit{Solar Concentrator Pointing Control System Development}, discusses the conception and development of a novel sensor technology to enable solar concentrators, coupled to fibre optic cables, to track the solar disk image on the concentrator focal plane. Initially the need for a pointing system is addressed, design requirements identified and classical control system theory reviewed. Then, the novel concept is introduced along with the necessary pointing control algorithms. Finally a practical investigation is summarised concerning the efforts undertaken to manufacture a working sensor and to demonstrate the sensor being used for feedback control of a two-axis concentrator pointing control mechanism.
Chapter 8, *Summary and Conclusions*, summarises the work undertaken and the novel results obtained from this research.

### 1.6 Novel Work Undertaken

In this work novel results have been produced in the following areas:

1. Concept, design, modelling and component testing of a fibre optic augmented solar thermal propulsion technology demonstration system for a small-satellite.

2. Concept and mission analysis of a fibre optic augmented solar thermal propulsion system for small-satellite that makes use of multiple thrusters of either direct gain and thermal storage regimes via optical switching to enable small-satellites to perform high propulsive performance missions.


Concept, design and testing of a novel sensor technology for solar tracking and optical switching using fibre optic luminescence.

### 1.7 Research Publications


Chapter 2

2 Literature Survey

2.1 Solar Thermal Propulsion Concepts and Heritage

Krafft Ehricke, a German-American space flight engineer, was the first to conceive the concept of solar thermal propulsion in 1956 [13, 1992]. By 1962 the first solar thermal rocket engine was successfully tested at the U.S Air Force Rocket Propulsion Laboratory (AFRPL) located at Edwards Air Force Base, California. This first test demonstrated the great potential of STP with an engine specific impulse of 680s using hydrogen as propellant [14, 1979]. This value of specific impulse is competitive by current spacecraft bipropellant engine standards, which are capable of approximately 340s. Specific impulse \( I_{sp} \) rates the amount of propellant required for a given change in velocity. It is defined as the thrust \( T \) divided by gravitational acceleration \( g \) per unit mass flow rate \( \dot{m} \) [15, 1992]:

\[
I_{sp} = \frac{T}{\dot{m}g} = \frac{I}{M_pg} = \frac{u_e}{g} \quad \ldots(2.1)
\]

where \( u_e \) is exhaust product velocity. Thus this first test of STP demonstrates that to produce the same impulse \( I \) as a modern state-of-the-art bipropellant engine an STP engine could potentially only require approximately half the mass of propellant \( M_p \). This increase in propellant efficiency is advantageous in terms of cost and can enable a given satellite to have a greater portion of its mass devoted to payload. Potential mass savings of propellant becomes more substantial for manoeuvres and space missions requiring larger changes in velocity increment \( \Delta V \). This relation is visible from the “rocket equation” devised by Tsiolkovsky [15, 1992]:

\[
\Delta V = u_e \ln \frac{M_o}{M_b} = I_{sp}g \ln \frac{M_b + M_p}{M_b} \quad \ldots(2.2)
\]

where \( M_o \) is the starting mass of the rocket, \( M_b \) is the burnout mass of the rocket, generally a combination of structure mass and payload mass and \( M_p \) is the propellant mass. For small \( \Delta V \) requirements moderate differences in specific impulse are relatively unimportant as the mass saving in propellant is only marginal. To demonstrate this, consider a micro-satellite with a burnout mass of 100kg and with a propulsion system capable of 50 m/s \( \Delta V \) for orbit maintenance manoeuvres. Using equation (2.2) a propellant mass saving of 0.85kg is incurred by employing a
propulsion system capable of 680s instead of 340s. However, the same satellite with a propulsion system capable of 4,200 m/s $\Delta V$ (typical for LEO-GEO transfer) would incur a mass saving of over 165kg. This mass represents a significant saving in cost and also could be used to allow for extra payloads. Furthermore an increased micro-satellite $\Delta V$ capability enables a wider range of micro-satellite missions. This comparison does not take into account the complexities of the varying propulsion systems capable of such performances but serves to demonstrate that for larger $\Delta V$ requirements STP becomes increasingly more appealing.

From the 1960's through to the 1970's the development of STP was delayed by investigations into other forms of high performance propulsion, such as solar electric and nuclear thermal concepts. Interest in the STP concept was increased by a technical report written by Etheridge in 1979 [14, 1979], which concentrated on the value of STP for high $\Delta V$ space missions. Etheridge analysed a number of STP configurations and identified key technologies that would enable a performance of up to 1000s specific impulse. Technologies such as large, low mass, inflatable concentrators and refractory metal windowed solar cavity receivers were employed to heat a particle laden hydrogen flow up to temperatures of 2,800K. The report concluded that STP was a viable option for high $\Delta V$ missions and offered a good compromise between an ion bombardment type propulsion system (long trip time/high payload mass) and a $LO_2-LH_2$ stage (short trip time/low payload mass). Since the appearance of the Etheridge report much research has been conducted into the key technologies identified regarding STP system components. Consequently a number of varying STP configurations employing these technologies exist. A relatively recent concept is the Integrated Solar Upper Stage (ISUS) program developed at the US Air Force Phillips Laboratory and ground tested at the National Lewis Research Centre (NASA-LeRC) around 1997, figure-(2.1). This STP configuration employed deployable, faceted concentrator arrays to gather the necessary power to the solar cavity receiver [16, 1996]. However, despite recent advances in technology and substantial past research a, solar thermal propulsion system is yet to be launched.

![Figure 2.1](image1.png)

Figure 2.1: (a) Integrated Solar Upper Stage. (b) NASA-LeRC tank six test facility [16]
2.1.1 Sunlight Concentration Techniques

The harnessing of concentrated sunlight is a very old concept and has been practiced for thousands of years; possibly as early as the clay-tablet era in Mesopotamia (~2000BC), when polished golden vessels were used to ignite altar fires [17, 1976]. Development of solar energy technology speeded up over the course of the 18th and 20th centuries. In 1747 astronomer Jacques Cassini constructed a lens 112cm in diameter that was capable of melting an iron rod, suggesting temperatures of around 1000°C. A common use of simple sunlight concentrating technology is for cooking food in arid/desert environments. The two most common methods of sunlight concentration are parabolic and Fresnel lenses/mirrors (figure (2.2)). Solar furnaces in the 19th century would employ faceted flat mirrors arranged in a parabolic manner such that they had a common focus. Modern technology allows the construction of mirrors with parabolic surfaces of excellent quality. The geometric concentration ratio (CRg) of a concentrating component is defined as [18, 2007]:

\[ CR_g = \frac{A_s}{A_f} \]  

where \( A_s \) is the concentrator collection area and \( A_f \) is the focal spot area. A large geometric concentration ratio indicates an ability to harness a large intensity of light. Thus the concentration ratio has a big impact on the final temperature of the solar energy receiver. If we consider a windowless cavity receiver, the size of the aperture in to the cavity (same as \( A_f \)) is reduced for larger concentration ratios of the concentrating optics, thus reducing radiative heat loss. The Stefan-Boltzmann law of radiation, is given by:

\[ P = \varepsilon \sigma T^4_f \]
where $P$ is the power radiated from a body of surface temperature $T_s$, surface area $A$ and emissivity $\varepsilon$. Equation (2.4) indicates that the smaller the area of a body being heated by an external source, the higher the temperature of the body when a power balance is struck between incoming power and radiated power.

Past STP configurations rely on the collection of large amounts of solar power to heat a propellant, which is typically hydrogen. Grossman and Williams [7, 1989], discuss the design of an STP configuration employing twin off-axis inflatable parabolic concentrators, having an elliptical rim semi-major axis of 40m (resulting in a solar concentrator collection area of $\sim 1,500 \text{ m}^2$), figure (2.3). The intensity of solar energy at the Earth's orbital radius is 1,353 W/m$^2$, suggesting the power collected by these twin concentrators is on the order of 2 MW. This collected power heats the hydrogen propellant to a temperature of 2,500°C, which when exhausted through the thruster produces 196 N of thrust. Grossman states that a concentration ratio of 10,000:1 is required to achieve the necessary propellant temperature.

![Inflatable parabolic concentrator design](image1)

![L'Garde Inc STP concept](image2)

**Figure 2.3:** (a) Inflatable parabolic concentrator design. (b) L'Garde Inc STP concept [7]

Thin film inflatable concentrators offer a mass per unit area of $<1 \text{ kg/m}^2$, which is promising considering the large collecting areas required [19, 2000]. What they lack is sufficient surface quality to attain a large enough concentration ratio. Pearson et al [20, 1999] reports a 1.14mm RMS (Root Mean Square) shape error, over a 2-year storage period, for a 5m inflatable concentrator. AFRL in conjunction with SRS and NASA have manufactured and tested inflatable concentrators of varying size. A number of inflatable structures have been manufactured and tested, including a flight test of an inflatable antenna. The Inflatable Antenna Experiment (IAE) was a joint venture between NASA and L'Garde Inc. A 14m diameter inflatable antenna, designed for millimetre-wavelength communications, was launch during a Space Shuttle mission in 1996. The experiment was intended to supply data on surface form errors and thermal stability over
Fibre Optic Applications in Solar Thermal Propulsion Systems

orbital periods. However, the antenna failed to inflate to a functional pressure and was only able to deploy due to residual gas within the inflation system [1, 2004] (Figure 2.4a).

An alternative concentrating system was investigated by NASA, in the form of the Shooting Star Experiment (SSE) (figure 1.1b), and consisted of a thin film, deployable, Fresnel primary concentrator. Due to the poor concentration ratio of the primary concentrator, a secondary concentrator was employed to gather the light into the thruster and increase the overall concentration ratio, (figure (2.5)) [21, 2000].

Through refraction and total internal reflection, light from the primary is channelled through a dielectric secondary concentrator, into the thruster, which reaches temperature in excess of 2,000 K. The secondary concentrator is typically manufactured from sapphire or zirconium, materials which are capable of withstanding such high temperatures. The secondary is capable of augmenting the primary concentration by an additional 20:1 ratio. This innovative high efficiency concentrator offered significantly higher throughput efficiency over other secondary concentration schemes, such as a hollow reflective secondary only offering an additional 7:1 concentration. Other advantages included the ability to tailor the radiative energy distribution upon the cavity wall and no requirement for active cooling [22, 1999]. This research is the first known attempt to
direct concentrated solar energy into an STP thruster via total internal reflection within a dielectric medium.

Alternative concentration system configurations have been considered over the years including solid precision surface concentrators, deployable petal structures and tensioned membranes. Although solid and deployable petal structures incur higher mass per unit area > 1 kg/m² this can be compensated by increased performance. The Integrated Solar Upper Stage program developed at the US Air Force Phillips Laboratory, see figure (2.1a), employed deployable, faceted concentrator arrays to power a thermal-storage thruster¹. The higher performance potential of thermal storage thrusters allowed the reduction in size of the concentrator array and compensated for the additional array mass [23, 2001][1, 2004].

2.1.2 Solar Thruster Heritage

Solar thermal propulsion is a monopropellant propulsion system and in order to provide a competitive performance must heat the propellant to high temperatures via an external means. Many of the concepts discussed in the previous section relied upon the collection of massive amounts of solar power to directly heat the propellant via the solar cavity receiver (know as a “direct gain” STP thruster). Therefore the power available to heat the propellant is limited by the collection area of the solar concentrators. In other chemical bi-propellant propulsion systems the energy available to heat the propellant is obtained through the decomposition or combustion of the propellant and no external heating system is required. These systems are designed such that heat transfer between propellant and the engine wall is minimised, whereas in a solar thermal propulsion system this heat transfer is maximised. Alternatively, for a STP system, the propellant can be heated directly from the concentrated solar radiation, this requires an enclosed cavity with a windowed aperture and “seedant” particles present within the propellant to increase absorption. These concepts require the concentrating system to be aligned with the sun during the manoeuvre [24, 1986].

A report written by Shoji et al [24, 1986] for AFRL, compares a variety of solar heat exchanger concepts in terms of performance, life, reliability and cost. Five concepts were traded, including:

- Windowless heat exchanger cavity: Solar energy is received through the cavity aperture and hydrogen propellant flows through the walls of the cavity. Maximum propellant temperature is limited to the maximum allowable temperature of the cavity wall material.
  Performance estimated at 900 s specific impulse.

¹ Thermal-storage solar thermal thrusters store energy from the incoming light in high heat capacity materials. Heat is then removed from the thruster by the propellant. This concept offers high performance as the power transferred to the propellant is not limited to just the power collected by the concentration system.
- Windowed particulate absorption: A seed or molecular constituent is mixed in to the propellant to directly absorb the solar radiation. A transparent window is employed to contain the propellant flow and is cooled by pure hydrogen, which also prevents deposition of the seed on the window. The absorbing component of the propellant has a high molecular weight, which restricts performance to 1,000 s specific impulse.

- Windowed rotating bed: Similar to the particulate absorption concept. A rotating porous cylinder creates a centrifugal force to retain the seed component of the hydrogen propellant. This concept requires high temperature bearings and seals and can provide a performance of 1,100 s specific impulse.

- Windowed alkali metal absorption: Easily ionised alkali metals such as lithium are introduced into the hydrogen propellant to create a radiation absorbing plasma. Again pure hydrogen is used to cool the window and prevent deposition of the alkali metal. The higher molecular weight of the alkali metals compared to hydrogen lowers the achievable specific impulse to 1,000 s. This concept was first introduced by Rault and Hertberg [25, 1983]

- Windowed graded porous material absorption: A series of porous refractory carbide disks within a regeneratively cooled cylindrical absorption chamber. These disks absorb solar radiation and transfer heat to the hydrogen propellant flowing through the pores of the disks. Porosity is optimised for propellant heat absorption with a reasonable pressure drop through the disks. A system performance of 1,050 s is estimated.

Figure 2.6: Various STP ‘direct-gain’ receiver concepts [24]
The windowless heat exchanger cavity had been previously identified as the most suitable for near-term proof-of-principle physical testing, owing to its relatively low mass, high reliability and low cost. Shoji et al [26, 1985] discusses in-depth the design and manufacture of a high performance ground-test direct-gain STP thruster. Rhenium was selected for the high temperature thruster due to its ability to withstand high temperatures while at the same time remaining ductile, weldable, and chemically inert with the propellant at high temperatures. see figure (2.7a). The solar absorber is a coiled rhenium tube and the thruster is a rhenium converging/diverging nozzle. Performance calculations provide a relation between final hydrogen propellant temperatures and delivered specific impulse shown in figure (2.7b). Smaller windowless STP thrusters were manufactured and tested at the National Aerospace Laboratory (NAL) of Japan [27, 2000]. These thrusters were designed for operation aboard 5–50 kg nano/microsatellites and were designed with receiver cavity apertures of size 10, 20 and 50 mm. Single crystal tungsten and molybdenum were used to construct these thrusters. Patented by the National Research Institute of Metals (NRIM) these unique materials exhibited no high temperature recrystallisation embrittlement. The testing of these thrusters demonstrated a performance of 750 s at temperatures as high as 2,000 K with nitrogen and helium gas as propellant.

![Ground test rhenium STP receiver.](image)

**Figure 2.7:** (a) Ground test rhenium STP receiver. (b) Hydrogen propellant temperature vs. performance [28]

An alternative approach to the direct heating of propellant via a heat exchanger or direct absorption was investigated for the Integrated Solar Upper Stage. This alternative approach employed high heat capacity materials to absorb and store the energy from the incident concentrated sunlight. This approach permits the ability to thrust without the concentrator system targeting the sun and offers higher thrust firings or smaller concentrator systems, as the power to the propellant is now only limited to what can be stored by the high heat capacity materials. The ISUS cavity receiver, referred to as the Receiver-Absorber-Converter (RAC), consisted of a graphite black body cavity with high temperature insulation around its exterior. The graphite thermal storage cylinder was coated with rhenium, protecting it from hydrogen sublimating at
high temperatures [28, 1999]. The ISUS RAC was ground tested at the Lewis Research Centre (NASA-LeRC) in 1997. Figure (2.1b) shows the Tank 6 facility at NASA-LeRC, which is a high vacuum chamber with a 30 kW xenon arc lamp solar simulator capable of simulating the sunlight environment in space. The peak specific impulse achieved by the ISUS RAC on July 22, 1997 was estimated from test data to be 742 s with hydrogen propellant [29, 1998].

Recent research conducted at the University of Surrey looked into the use of high heat capacity ceramics for thermal storage STP cavity receivers. Such materials have exceptionally high heat capacities at high temperatures (2,000 K) and are resistant to chemical attack from nitrogenous and hydrogenous compounds. TiB2/BN and zirconia-strengthened boron nitride (ZSBN) cavity receivers were successfully tested at a temperature of 2,000 K and subjected to helium, nitrogen and ammonia propellant at temperatures as high as 1,700 K [30, 2002]. These receivers later demonstrated experimentally a specific impulse of 237 s, when utilising ammonia propellant [1, 2004].

2.1.3 Propellants for Solar Thermal Propulsion

For STP concepts from the 1960s through to the present, hydrogen has been the propellant of choice. The reason for this is due to hydrogen having the lowest molecular mass ($M$) available, and for a given chamber temperature ($T_c$), provides the highest specific impulse ($I_{sp}$) in comparison to any other propellant. This is demonstrated in the following relation [15, 1992], which is discussed further in chapter 6:

$$I_{sp} = \sqrt{\frac{2y \bar{R}}{(\gamma - 1)M} \frac{T_c}{P_e} \left[1 - \left(\frac{P_e}{P_c}\right)^{\gamma - 1}\right]} \quad \ldots \ldots (2.5)$$

where $\gamma$ is the ratio of specific heats, $\bar{R}$ is the universal gas constant, $P_e$ is the propellant exit pressure and $P_c$ is the propellant chamber pressure. Equation (2.5) demonstrates that for smaller
molecular weights higher exhaust velocities are possible, granting greater performance, for a
given temperature. Although hydrogen provides high performances difficulty is experienced in
the storage of hydrogen. With a low storage density of 71 kg/m\(^3\), hydrogen requires large storage
tank volumes to provide enough volume for the required mass. Tank volume is typically
minimised by storing liquid hydrogen in a cryogenic tank but this requires substantial power to
maintain. Also any heat transfer to the liquid hydrogen could result in boil off and disastrous
increases in tank pressure. This is more the case for the long-term storage of hydrogen envisioned
by STP concepts. Subsequently hydrogen storage tanks tend to be large, heavy and power hungry.

Returning again to considering the ISUS, Cady and Olsen [31, 1996] proposed a design for a
Cryogenic Storage and Propellant Feed System (CSPFS) for use with the ISUS. The CSPFS is
required to provide efficient storage and delivery of liquid hydrogen to the STP system for a 30-
day mission duration. A multilayer-insulated tank is used to prevent liquid hydrogen boil-off. A
zero-g Thermodynamic Vent System (TVS) subcools and collects counter flowing liquid
hydrogen in a Liquid Acquisition Device (LAD), eliminating a need for a pressurisation system.
Additional heat from the tank is removed by subcooled hydrogen passed through a heat
exchanger. All the vent flow from the TVS is used in the STP engine and none is jettisoned.
Chato et al [32, 1998] discuss the requirement for space testing of the TVS device despite
extensive ground testing. Space testing was deemed necessary due to the TVS sensitivity to low
gravity and the design of suitable equipment to test the storage concept was proposed in the form
of the Hydrogen On-Orbit Storage And Supply Experiment (HOSS). This included the design of a
36 to 80 litre dewar, depicted in figure (2.9):

![Figure 2.9: HOSS experiment dewar [32]](image)

The study conducted by Chato demonstrated the availability of insulation and guard techniques
for ground processing capable of storing liquid hydrogen in the dewars without venting in excess
of 144 hours.
The density specific impulse ($Dl_{sp}$) is a common parameter used in the selection of a propellant for use with a small-spacecraft propulsion system. It is defined as the product of ideal specific impulse and the propellant specific gravity (the ratio of the propellant density and the density of water) and provides a good indication of the volumetric compatibility of the propellant. A high value of $Dl_{sp}$ is important for compact propulsion systems, which are constrained by the spacecraft's volume [33, 2001]. Kennedy [1, 2004] compares a selection of common propellants in terms $I_v$ as calculated from equation (2.5) and $Dl_{sp}$, these are shown in table (2.1):

<table>
<thead>
<tr>
<th>Propellant</th>
<th>Density ($g/cm^3$)</th>
<th>$I_v$ (s)</th>
<th>$Dl_{sp}$ (g-s/cm$^3$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>STP H$_2$</td>
<td>0.0710</td>
<td>917</td>
<td>65</td>
</tr>
<tr>
<td>STP NH$_3$</td>
<td>0.6000</td>
<td>449</td>
<td>270</td>
</tr>
<tr>
<td>STP N$_2$H$_4$</td>
<td>1.0045</td>
<td>402</td>
<td>404</td>
</tr>
<tr>
<td>STP H$_2$O</td>
<td>1.0000</td>
<td>372</td>
<td>372</td>
</tr>
<tr>
<td>MMH/N$_2$O$_2$</td>
<td>1.1590</td>
<td>319</td>
<td>370</td>
</tr>
<tr>
<td>H$_2$O$_2$/Kerosene</td>
<td>1.2790</td>
<td>305</td>
<td>390</td>
</tr>
</tbody>
</table>

In table (2.1) the specific impulses stated for the STP monopropellant options are for a propellant temperature of 2,500K. Furthermore, in table (2.1) two common bi-propellant options have been considered (MMH/N$_2$O$_2$ and H$_2$O$_2$/Kerosene) for comparison to the STP monopropellant options. For both the bi-propellant options the oxidiser to fuel ratio is optimised for maximum specific impulse [1, 2004]. When comparing the specific impulses of the propellants in table (2.1) it is clear that hydrogen outperforms the other propellant options. However, when comparing the density specific impulses of the propellants in table (2.1) it becomes apparent that for volume constrained spacecraft, hydrogen is not a desirable option, due to its small $Dl_{sp}$ in comparison to the other propellant options. The other propellants in table (2.1) are much more storable than hydrogen, due to their higher density and the relative simplicity of their storage and feed systems. Ammonia offers particularly simple storage and feed system, with its ability to self pressure regulate the storage tank at its own vapour pressure and without any requirement for storage tank heating. The decision as to the most appropriate propellant for a small-spacecraft is an engineering exercise dependant on desired system performance and system complexity. The other STP options are considered further in chapter 3, however, table (2.1) serves to demonstrate the viability of using more storable propellant options for an STP system onboard a small-spacecraft.

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2. "Ideal" here refers to a perfect expansion to vacuum, such that in equation (2.5) the exit pressure ($P_e$) is zero.
2.1.4 Solar Tracking Systems

Limited information is available for concentrator pointing control concepts of past STP configurations, mainly due to research programs focussing more on concentrator and cavity receiver aspects of the STP systems. Past “direct-gain” concepts in which power is transferred directly from the concentrator array to the propellant via a heat exchanger often employed twin off axis inflatable parabolic concentrators as discussed by Grossman [7, 1989] of L’Garde Inc, see figure (2.3b). Grossman describes a pointing mechanism capable of two-degrees of freedom pointing of the twin concentrators coupled with the attitude system, enabling continuous sun tracking during a manoeuvre. A control system is described that is capable of sensing the location of the concentrated sun-spot with respect to the engine aperture via heat/temperature sensors located on a turntable around the absorber aperture. Grossman states that the concept was not final and other methods were being investigated.

Partch [28, 1999] describes a two-axis gimbal mechanism for pointing motions, for the Solar Orbit Transfer Vehicle (SOTV) space experiment, a follow on concept to the ISUS, see figure (2.10a). Partch identifies a worst-case solar alignment error of 0.1°. AFRL research into inflatable concentrators involved the use of the Tank 6 solar thermal vacuum facility at NASA’s Glen Research Centre (GRC). The inflatable concentrator tested in this program exhibited excellent slope error (2 mrad) and collection efficiency. The concentrator struts were mounted on a 6-degree-of freedom, electrical-actuator-driven, remote-controlled base. ATK Thiokol Propulsion developed this hexapod pointing system, see figure (2.10b).

Terrestrial and space-borne solar tracking devices for solar concentrators are required to have active control due to the fine pointing accuracies incurred through the concentration of solar
energy at the focal point of the concentrator. Typically a photovoltaic sensor is employed to estimate the Sun vector. Roth et al. [34, 2005] describes a cheap two axis terrestrial Sun following device, which employs a photocell detector to obtain a tracking accuracy of 0.5°. Other terrestrial solar trackers rely on assumptions of device orientation and an initial manual alignment [18, 1985]. A common solar tracking actuator for parabolic trough concentrators is a single axis passive thermo-hydraulic drive system [35, 2004].

Lovegrove [37, 2000] discusses the design and construction of a paraboloidal dish power station, figure (2.11b). The dish employs a commercial control system (Opto 22™) to calculate the sun vector via open loop tracking and a walking hydraulic ram for actuation.

Good solar pointing/tracking accuracies are not only required for solar concentrators, especially for solar observatory spacecraft whose various specialist instruments require good pointing accuracy and a low local vibration environment. An example of this is the Michelson Doppler Imager (MDI), an instrument on board the Solar and Heliospheric Observatory (SOHO), a joint venture between NASA and the European Space Agency (ESA)[38, 1995]. The SOHO spacecraft, launched in 1995, is a large spacecraft weighing over 1,800 kg. It is located in a halo orbit about the L1 Sun-Earth Lagrangian point. In order to provide a stable platform from which the instruments it accommodates can operate, SOHO employs a 3 axis stabilised attitude control
SOHO is capable of pointing at the Sun within an accuracy of 10 arc seconds and can maintain a pointing stability of 1 arc-second over a 15 minute interval [39, 2004]. The MDI is used to probe the interior of the Sun via observations of the Sun's photospheric layer. Despite the pointing and stability offered by the SOHO platform the MDI instrument has an integrated pointing and imaging stabilisation system. This is a closed loop control system consisting of four orthogonal detectors at the guiding image focal plane that command a 3-point piezoelectric transducer actuated mirror to remove errors in the observed limb position [39, 2004].

The PICARD microsatellite, slated for launch in 2008, is one of the first microsatellites to be developed by Centre National d'Études Spatiales (CNES). PICARD is a solar observatory mission focussing on obtaining accurate measurements of the Solar diameter, differential rotation and solar constant intensity in an effort to investigate how each varies and impacts upon the others [40, 2007]. The 150 kg PICARD platform will orbit the Earth in a 6am-6pm sun-synchronous orbit at an altitude of 750 km. This orbit selection should maximise PICARDs exposure to the Sun, at the given altitude, with only a few short eclipses predicted over the course of a year. The main payload of PICARD is the Solar Diameter Imager and Surface Mapper (SODISM). SODISM is a whole Sun imaging telescope, designed to accurately measure the solar diameter and shape [40, 2007].

![Figure 2.12: (a) SODISM instrument CAD model (b) SODISM instrument component layout [40]](image)

The SODISM instrument is composed of two solar telescopes; an imaging telescope and a pointing telescope, as seen in figure (2.12b). At the focus of the pointing telescope a quad-cell image displacement sensor is located. The signals from this sensor are interpreted into pointing control information that is used by the satellites 3-axis stabilised attitude system to facilitate fine solar pointing and also by 3 piezoelectric actuators, in a control loop, to tilt the primary imaging mirror in order to compensate for stability errors. PICARD platform is capable of solar pointing accuracies of ±0.01° and the SODISM instrument is capable of pointing stabilities of 0.1" [40, 2007].
2.2 Fibre Optics for the Transmission of High Intensity Sunlight

2.2.1 Solar Radiation

The Sun is generally regarded as a blackbody radiating at a temperature of approximately 5,800K. A blackbody is defined as an object that absorbs all electromagnetic (EM) radiation that falls on to it and also does not allow radiation to pass through it or reflect from its surface. A blackbody also radiates at all wavelengths in a manner described by Planck's blackbody radiation law:

\[
S_\lambda = \frac{8\pihc}{\lambda^5} \frac{1}{e^{hc/\lambda kT_b} - 1} \quad (2.6)[41]
\]

where \( S_\lambda \) is the energy per unit volume per unit wavelength (\( \lambda \)). Also \( h \) is Planck's constant, \( c \) is the speed of light, \( k \) is Boltzmann's constant, and \( T_b \) is the blackbody temperature in degrees Kelvin. When comparing the measured EM spectrum of the Sun to that of a theoretical blackbody radiating at the same temperature of the Sun, it is clear why the Sun is often described as a blackbody radiator, figure (2.13):

![Figure 2.13: Theoretical and experimentally measured solar radiation plots [42]](image)

The measured solar EM spectrum as seen in figure (2.13) is referred to as the Air Mass Zero (AM0) curve, indicating that this spectrum was obtained from outside the Earth's atmosphere. Other terrestrial solar EM spectrum curves are catalogued and referenced via an air mass number, which describes how much atmosphere the solar radiation has passed through, which is dependent on the solar zenith\(^3\) angle at which the spectrum was obtained [18, 1985]. Terrestrial solar spectra experience absorption bands in the Ultra-Violet (UV)(below 400 nm), between the visible and the near Infra-Red (IR)(800-1,100 nm) and further into the IR (1,200 nm and 1,300 to 1,500 nm). These absorption bands are primarily due to water and carbon dioxide absorption [18, 1985]. The differences between extraterrestrial and terrestrial solar spectrums have consequences for the

\(^3\) The zenith is the direction pointing directly above a particular location.
terrestrial testing of STP systems. The most significant difference is that the solar power available terrestrially is significantly reduced. The Sun emits $3.65 \times 10^{26}$ W of radiative power into space [17, 1976], the intensity of which on reaching the Earth has reduced from a local solar surface intensity of $\sim 6 \times 10^7$ W/m$^2$ to $\sim 1,353$ W/m$^2$ (the solar constant). This intensity is then further reduced upon traversing the Earth's atmosphere. Furthermore the various absorption bands incurred though atmospheric transmission are not present in space, subsequently exposure of STP components to these solar spectrum radiation bands should be taken into account. Solar lamps are often employed to simulate extraterrestrial solar conditions. These lamps tend to be xenon-arc lamps, which are capable of operating at filament temperatures of up to 6,000 K. Spectral filters are often employed with these lamps to rid the output spectrum of xenon peaks [43].

Another consideration is the size and intensity distribution of the solar disk. Although the solar radius is much smaller than the separation between the Sun and the Earth (the astronomical unit (AU)$^4$), by a factor of $\sim 1,000$, the Sun still subtends a substantial angle at the Earth ($\sim 0.25^\circ$). Therefore considering the Sun as a point source is not representative when examining concentrator systems. Furthermore the actual intensity of the solar disk as seen from the Earth varies as a function of distance from its centre. This effect is known as Limb Darkening. Nicolás [44, 1987] identifies several common models of the solar disk intensity distribution as seen in

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$^4$ The astronomical unit is the distance between the Sun and the Earth. 1 AU = 149,598,000 km
figure (2.14a). The models are normalised based on the intensity received at the Earth. The square limb-darkening plot in figure (2.14a) is the simplest with the solar intensity being invariant with respect to viewing angle. The uniform plot is most similar to the real limb-darkening distribution, which assumes the local intensity of the Sun is uniform but that the Sun is also a lambertian source. The final limb-darkening model Nicolás identifies is a gaussian distribution, which processes the largest central intensity.

Negi et al [45, 1986] compares several existing numerical correlations of the limb-darkening distribution and presents a new correlation based on an existing polynomial form originally proposed by Lipps and Walzel [46, 1978]. Negi et al perform a least squares regression analysis using the experimental data to obtain the required polynomial coefficients. In figure (2.14b) the correlation proposed by Negi et al (labelled “Present Correlation”) is a good match to the “Experimental” data, as would be expected.

2.2.2 Previous Work

The concept of applying optical fibres to the transmission of solar radiation is relatively young. The earliest record found, is a paper submitted by Kato and Nakamura to the Journal of Applied Physics [47, 1976] in 1976. In this paper they discuss the application of optical fibres to the transmission of broad-spectrum solar radiation. Kato and Nakamura identify that the exploitation of pure fused silica and high silica content glass for optical fibres has led to remarkable reduction in light attenuation. Applications such as low cost earth based solar power plants, solar heating and cooling of buildings; under water energy transmission and high power-density spacecraft bound photovoltaic solar cells are suggested. Kato and Nakamura discuss the attenuation effects typical of low-loss silica fibre optics and describe the main intrinsic cause to be the presence of high O-H (Hydroxyl Ion) concentrations and defects within the core material. Based on these intrinsic attenuation mechanisms Kato and Nakamura show that fused silica optical fibres can transmit concentrated solar radiation effectively over distances of 40 m, see figure (2.15a).

In figure (2.15b) we see the AM0 solar spectrum superimposed over the attenuation spectrum of a modern low-OH fused silica fibre optic cable [48, 2007], indicating fused silica’s suitability for transmitting solar radiation. The small peak observed in figure (2.15b) at ~1,400 nm is an OH absorption peak.

---

5 A Lambertian source is an optical source that obeys Lamberts cosine law, i.e., that has an intensity directly proportional to the cosine of the angle from which it is viewed.

6 The Hydroxyl Ion (O-H) is representative of water being present in the atmosphere during the manufacture of the fibre optic.

7 The AM0 spectrum is the solar spectrum experienced in space. Other spectrums, AM1, AM1.5 etc, are solar spectrums experienced through the Earth’s atmosphere at varying angles.
The earliest record of an effort to transmit high intensity solar energy through a fibre optic cable is reported by Cariou et al [49, 1982] in 1982. Cariou describes the use of a small parabolic concentrator to direct light in to a 10m long fibre optic cable. A transmitted power of 2 W at a concentration of 3,000 is reported demonstrating a total efficiency of 70%. Further research conducted by Liang et al [50, 1998] in 1998 demonstrated a total efficiency of 60% when transmitting 60 W of power over a 3 m long anhydroguide\(^8\) PCS low OH Vis-IR fibre. This fibre possessed an acceptance half angle of 26°. A 40.6cm diameter parabolic primary concentrator was used to focus 100W of intercepted solar light, via a secondary mirror and an aspheric lens, into a fibre bundle made up of 19 1.5mm diameter fibre optics, at a concentration of 4,500 see figure (2.16a). Liang et al attributes 20% of the power loss to reflections at the primary and secondary mirrors.

Gordon et al [51, 2003] has investigated numerous applications of fibre optics for transmission of solar radiation. These include solar surgery, solar generation of nanomaterials, and solar electric power. Featuring in several of Gordon et al publications [51, 2004][52, 1999] is the solar fibre-optic mini-dish, see figure (2.16b), which effectively couples light from the dish to the optical fibre. Using a mini-dish of 20-cm diameter, capable of a high concentration value of ~12,000, Gordon et al transmitted 8 W of power over a 20 m long fibre optic, demonstrating a total efficiency of 64%. This fibre was a fused silica fibre with an acceptance half angle of 41.3°. On analysing the system, Gordon et al found that there was more observed power loss (loss by a factor of 0.80) than is attributable to the individual component efficiencies. Gordon et al states that the power loss attributable to fibre optic attenuation, should be negligible according to the manufacturers attenuation spectrum, but despite this, similar power loss has been observed in other laboratories. (Liang et al [53, 1997] and Irvin and Nakamura [54, 1991]). Gordon et al and

\(^{8}\) A fused silica core fibre optic with a doped silica cladding.
Feuermann et al [55, 2002] describe a phenomenon referred to as “light leakage”, which Gordon et al states is a consequence of imperfect total internal reflection within the fibre optic cable and is responsible for the observed loss. Gordon et al continues to demonstrate that the supposed power loss attributable to “light leakage” is greater for higher concentration systems. Gordon et al predicts a maximum realizable fibre transmission efficiency, including “light leakage” loss, of 80% for fibre optics with large acceptance angles, and identifies the importance of selecting an appropriate concentrator rim angle9, which is dependant on the numerical aperture $(NA)$ of the fibre optic, related to the sine of the fibre’s acceptance half-angle $\psi_a$.

$$NA = n_e \sin \psi_a = \sqrt{n_{core}^2 - n_{clad}^2} \quad \cdots (2.7)$$

Where $n_e$ is the external refractive index and $n_{core}$ and $n_{clad}$ are the refractive indices of the fibre core and fibre cladding. The numerical aperture of the fibre should be matched with the rim angle of the concentrator such that all light from the concentrator is able to pass through the fibre. Also the focal spot size attainable with the concentrator should be matched with the fibre bundle’s diameter for efficient coupling between the two. In order to achieve the 80% efficiency previously mentioned Gordon stresses the importance of accurate alignment of the fibre tip with the focal spot and the use of a highly accurate sun pointing mechanism. Feuermann and Gordon estimate an absorption of 7% over a 100m length of fibre [52, 1999], this figure was based on absorption in the cladding material for fibres with small numerical apertures, although, the actual value was not reported. From the literature it remains unclear as to the difference between expected and observed system efficiencies.

9 This is the angle between the focal axis and the line between the focal point and the mirror rim.
2.2.3 Fibre Optic Optical Switching

An optical switch is a device commonly employed in the telecommunication industry, which enables signals in optical fibres or integrated optical circuits to be selectively switched from one circuit to another [47, 2001]. An optical switch may function via mechanical means, such as physically shifting an optical fibre termination to couple with one or more fibres, or by electro-optic effects and magneto-optic effects. Mechanical optical switches are generally used for signal re-routing, whereas the other methods are used to perform logic operations within optical circuits.

In the context of this work mechanical optical switches are of interest as they could provide the ability to select the destination of concentrated solar energy in a STP system, such that a single concentrator system could be employed to heat more than one solar thermal thruster. Mechanical optical switching devices for fibre optics transmitting high intensity solar radiation have not been found within the literature, however, it is informative to consider current state-of-the-art mechanical switches employed within communication systems. Jia et al [57, 2007] discuss a novel optic-fibre switch based on a Micro-Electro-Mechanical System (MEMS) electromagnetic microactuator. A series of wobble-type MEMS optical switches are designed, fabricated and tested (figure (2.17)). Switching times of less than 5 ms are reported with insertion losses of 0.5 dB.

Fan et al [58, 2005] present the design, fabrication and tests of a miniature low cost high reliability mechanical optical switch, also using MEMS technology. This technology is employed to switch between single-mode fibres with a core diameter of 9 μm with position errors of less than 0.1μm and switching times of around 5ms. Reliability tests of the optical switch demonstrated insertion losses of approximately 1.2 dB ± 0.04 dB after 10,000 cycle times.

2.2.4 Optical Fibres for Space Based Applications

Nakamura has contributed greatly to the concepts involving fibre optics for space applications. In 2002 Nakamura et al [59, 2002] report on the development of an Optical Waveguide Solar Plant (OWSP) system. Light collected by solar concentrators and transmitted through fibre optic cables
is utilised for materials processing, plant lighting and solar heating on board spacecraft, as shown in figure (2.18a).

A rigorous component testing strategy was devised and undertaken for the OWSP system through the manufacture of a prototype, figure (2.18b). Four concentrators with rim angles matched to an optical fibre acceptance angle of 26° (NA=0.44) were coupled to a hexagonal shaped bundle of 37 fused silica core fibres. The concentration ratio achievable by these concentrators was limited to 7,500 by the fibre NA. A special vacuum chamber constructed for the fibre bundle was employed to determine the expected bundle tip temperature when operating in the space environment. When ground tested, the fibre bundle vacuum temperature reported was ~450°C. Given this result Nakamura estimates a maximum tip temperature of 725°C during space operation. This figure falls below the continuous service temperature of fused silica fibre optics. Total system efficiency was reported of 65% over a 15m fibre bundle. Nakamura identifies several technological issues requiring further development, including [59, 2002]:

- Lightweight primary concentrator with solar tracking system.
- Efficient secondary concentrator.
- Lightweight space qualified fibre optic.
- Efficient cable connector and switching devices.
- Efficient furnace for material processing or light distribution device for plant lighting.

In 2004 Kennedy and Henshall [11, 2004] and Nakamura et al [10, 2005] suggest the use of fibre optics to augment a solar thermal propulsion system. Both concepts exploit the ability to use multiple concentrators to heat a single or multiple cavity receivers. Testing conducted by Nakamura has demonstrated fibre optic heating of a solar receiver with a solar receiver reaching temperatures as high as 1,500 K [10, 2005]. Special quartz connectors were constructed for the four fibre optic cables employed to heat a graphite receiver. Each fibre was coupled to a 50 cm diameter parabolic concentrator mirror. These are the same concentrators as seen in figure 30.
(2.18b). A working fluid of argon and helium was also heated, up to 1,350 K, from the graphite receiver demonstrating heat transfer. However, the receiver itself was not designed to be a solar thermal propulsion receiver, and was only a thermal reactor originally designed for material processing, therefore no propulsive performance parameters were reported. This research proves that fibre optics can be used for the efficient transfer of solar energy to heat a working fluid. However, although Nakamura identifies the need for solar tracking and a desire to perform optical switching of the transmitted solar energy it is not demonstrated practically nor was a method of doing so indicated.

2.3 Coupled Radiation and Conduction Heat Transfer

Coupled conductive and radiative heat transfer within participating media is an important area of study for many industrial applications. Porous, fibrous and semitransparent materials are examples of media that can exhibit thermal behaviour in which radiative heat transfer plays an important role. Subsequently numerous cases of this type of problem for varying mediums and geometries have been considered in the literature. In modelling such problems, a non-linear integrodifferential energy equation is formulated, which is typically a complicated matter to solve. Some of the earliest references discussing these problems date back to the 1960s for example Viskanta and Grosh [60, 1962] apply a numerical integration and iteration technique to obtain the temperature distribution of a plane layer of grey gas between infinite, black parallel plates. Since then a variety of other techniques have been employed to solve a myriad of problems, which consider different material properties and different geometries. For example: Tan et al [61, 2004] employs a nodal analysis based on Hottel’s zonal method to treat transient coupled radiative and conductive heat transfer within non-grey semi-transparent materials. Wu and Liou [62, 1997] uses a finite difference algorithm combined with a discrete ordinate method to a two-dimensional, cylindrical, scattering medium which is subjected to heating both internally and at the geometry boundaries.

The Verlet algorithm was devised by L. Verlet in the early days of dynamical molecular simulation [63, 1995]. The leapfrog algorithm is a modified version of the Verlet algorithm and will be shown later in this work to be applicable to coupled radiation and conduction heat transfer problems. These types of algorithm are well known and are usually applied to control and dynamical problems. A typical control application of a leapfrog algorithm is to determine optimal linear quadradic regulator and Kalman filtering gains. Typically, the algorithm takes two related variables, such as position and velocity, and computes them at alternate half time-step intervals via a third order Taylor expansion. The value of one variable at a particular time acts as the initial condition for the other variable for the next time step. Second order accuracies can be attained
even for simple solutions to the equations of the related variables. Leapfrog algorithms have the further advantages of being time reversal invariant and symplectic\footnote{In mathematics symplectic integrators are used for the numerical integration of Hamilton equations. They are a class of geometric integrators which preserve the geometric properties of the exact flow of a differential equation.} [64, 1997].

2.4 Chapter Summary and Conclusion

2.4.1 Chapter Summary

This chapter has provided a history of the concepts of solar thermal propulsion and fibre optic transmission of solar radiation. These technologies have recently found a common ground in the form of a solar thermal propulsion system augmented by fibre optics.

STP has been under development for over 40 years. The research originally undertaken was commonly a costly venture due to the requirement to attain a high system performance on the order of 1,000 s Isp. This invariably required the use of very large inflatable/deployable solar concentrators that could collect a massive amount of solar power. This power was delivered to a heat exchanger designed to operate at temperatures as high as 3,000 K. The propellant selected was hydrogen, which at such high temperatures would deliver the required performance. Subsequently this demanded the design of large-scale spacecraft, dominated by their propulsion systems. The designs of these systems harboured many technological unknowns. Specifically: inflatable structures, high temperature ceramics and the storage of cryogenic hydrogen. Rockwell, AFRL and other agencies devoted resources to the development of these technologies and specific hardware, such as inflatable concentrators and solar cavity receiver heat exchangers, were manufactured. However, despite substantial research and development a solar thermal propulsion system is yet to be flown.

More recently the development of fibre optics for solar energy transmission has received a great deal of attention. Typically light collected by a parabolic mini-dish is focused onto the tip of a fused silica fibre optic bundle. Total system efficiencies as high as 70% have been demonstrated practically for reasonably large distances, on the order of tens of meters. Vacuum heating tests also demonstrated the survivability of fused silica fibres in a space environment.
2.4.2 Chapter Conclusions

On reviewing the literature the following conclusions were made:

- From the literature it appears that as research has progressed STP concepts have evolved from direct-gain concepts to thermal-storage concepts. The reason for this appears to be the smaller required concentrator sizes and greater freedom in orbital manoeuvring strategies. It was initially unclear as to which system concept is most suitable for a fibre augmented STP system, however, it was clear that direct-gain concepts would have the most to gain if fibre optics could alleviate concentrator and orbital manoeuvring issues. This is discussed further in chapter 3.

- Although hydrogen propellant was the popular choice for propellant in past STP systems, more common and more storable propellants were found to be most appropriate for use on a small-spacecraft. In terms of density specific impulse, most common small-spacecraft propellants out perform hydrogen (DIsp$_{\text{H}_2} = 65$ g-s/cm$^3$) with hydrazine (DIsp$_{\text{N}_2\text{H}_4} = 404$ g-s/cm$^3$) performing best overall. This is discussed further in chapter 3.

- In all but one of the past STP concepts in the literature, point focus parabolic concentrator optics have been employed for the concentration of solar energy. This is discussed further in chapter 4.

- Efficient fibre optic transmission of concentrated solar energy has been demonstrated in the literature and has included the use of such energy to heat a working fluid of argon and helium to very high temperatures (1,350 K). Overall, system efficiencies of between 60% and 70% have been reported. These values included the transmission loss of the fibre optics, which have been reported to be larger than expected in several publications. Based on the literature an overall system efficiency of greater than 70% is likely to be unattainable. This is discussed further in chapter 5.
Chapter 3

3 Mission Analysis and STP System Variants

In this chapter two categories of solar thermal propulsion system are identified, namely thermal-storage and direct-gain. The differences between these types of STP system are discussed in detail and missions applicable to each system are identified and reviewed. The benefits of augmenting both types of STP systems with fibre optics are discussed and new STP concepts are introduced which bring together the benefits of both types of STP system. A case study is performed to demonstrate the feasibility of employing an STP system on a platform as small as a micro-satellite as a main propulsion system or as an STP demonstration experiment. Finally a number of platforms suitable for a fibre augmented STP technology demonstration are identified.

3.1 Preliminary Mission Analysis

In this discussion the advantages and disadvantages of thermal-storage and direct-gain STP systems are considered. Several missions in which STP is applicable are identified and categorised into either thermal-storage or direct-gain. For each mission STP systems are traded against competing conventional systems. Satellite platforms are categorised into mass categories as follows:

<table>
<thead>
<tr>
<th>Category Name</th>
<th>Mass Range (kg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Macro-satellite</td>
<td>&gt;500</td>
</tr>
<tr>
<td>Mini-satellite</td>
<td>500-100</td>
</tr>
<tr>
<td>Micro-satellite</td>
<td>100-10</td>
</tr>
<tr>
<td>Nano-satellite</td>
<td>10-1</td>
</tr>
</tbody>
</table>

3.1.1 Standard STP Options

In chapter 2 the two general types of STP were identified, namely direct-gain and thermal-storage. To reiterate what these terms mean:

- Direct-gain: This term refers to the direct and immediate heating of the propellant via a heat exchanger which is accepting concentrated solar energy from the concentrator system. Here the power available to heat the propellant is limited to that collectable by the solar concentrators. The heat exchanger is typically heated to its operating temperature in
Fibre Optic Applications in Solar Thermal Propulsion Systems

a short amount of time (~400 s, see section 6.2.2), as it is not required to store thermal energy, rather immediately transfer it to the propellant, this requires that the receiver have a high thermal diffusivity\(^{11}\). The solar concentrators must be orientated at the Sun for the duration of a manoeuvre.

- Thermal-storage: This term refers to the storage of solar energy in a high heat capacity cavity receiver. The concentrators heat the receiver to high temperatures whilst pointing on-Sun. Thus the power available is only limited to that storable within the receiver. Long periods are typically required to heat the cavity receiver (>6,000 s). For example Kennedy [1, 2004] sizes several thermal storage receivers for a range of small-spacecraft missions requiring over 1,000 m/s \(\Delta V\), each receiver required over two hours of heating to attain its designed peak temperature. When the thermal storage receiver has reached its peak temperature the spacecraft orientates itself such that the receiver can thrust in the desired direction.

Each of these types of STP systems has consequences regarding application to a micro-satellite, which are now discussed; some of the disadvantages of each system can to a degree be alleviated with the application of fibre optics.

Direct-gain STP, is power limited by the size of the concentrator array. This limits the thrust deliverable by the system for a given performance. For early STP concepts, such as that shown in figure (2.3b), this explains the need to have such large concentrators in order to obtain the necessary performance and still provide sufficient thrust levels. In that particular direct-gain STP concept a thrust of 196 N was required, necessitating the large concentrators. For micro-satellites thrusting requirements are typically much smaller (<10 N), for example Kennedy and Palmer size a STP receiver to provide 3N of thrust for a 1000s duration manoeuvre of a micro-satellite [1, 2004]. Consequently a STP system for a micro-satellite would employ a suitably scaled concentrator. However, micro-satellites are often restricted in terms of their physical size, mass and complexity. A typical micro-satellite facet area is approximately 0.5 m\(^2\) (the UK-DMC micro-satellite has dimension 640 mm \(\times\) 640 mm \(\times\) 680mm); a circular concentrator of this size will collect approximately 260 W of solar energy (solar intensity = 1,370 Wm\(^{-2}\)). For this level of power \(P\) a thrust can be estimated by using the standard formula for electrothermal propulsion [2, 1999]:

\[^{11}\] Thermal diffusivity \((\alpha = kC_p/\rho^2)\) describes the rate at which heat is conducted through a medium and is the ratio of the medium’s thermal conductivity \((k)\) and the product of the medium’s specific heat capacity \((C_p)\) and density \((\rho)\). Therefore a medium with a high thermal conductivity and low density and specific heat capacity will rapidly conduct heat throughout the medium.
\[ \eta P = \frac{1}{2} T g I_{sp} \quad \text{...(3.1)[2]} \]

Where \( \eta \) is a conversion factor of electric energy to thermal energy. Equating \( \eta \) to the efficiency at which a STP system collects light and delivers it to the cavity receiver a direct-gain system of this concentrator area has a maximum thrust of 90 mN for a specific impulse of 350 s and at an efficiency of 0.6. This suggests that a micro-satellite direct-gain system would be suited to low/continuous thrust applications. The time taken to heat a direct-gain STP system that has a concentrator array that is static with respect to the satellite has a very limited range of applications. This is because a direct-gain STP system operates while the concentrator system is orientated at the Sun. Assuming the direct-gain receiver is located at the focal point of the concentrator system and that the receiver thrust direction is parallel to the concentrator optical axis then the thrust vector is restricted to be aligned with the Sun vector.

![Figure 3.1: Fixed concentrator direct-gain STP system configuration. (a) Thrust vector parallel to concentrator optical axis. (b) Thrust vector perpendicular to concentrator optical axis.](image)

This is demonstrated in figure (3.1a) where a fixed concentrator direct-gain STP system configuration is depicted. From figure (3.1a) it is clear that for the system to impart non-rotational motion to the spacecraft, the thrust vector must intersect the centre of mass of the spacecraft and must also be parallel to the optical axis of the concentrator. If the direct-gain receiver thrusts in a direction other than parallel to the optical axis of the concentrator, as seen in figure (3.1b), this will cause rotation of the spacecraft, which will miss-align the optical axis of the concentrator with the Sun vector. The only real application of this type of system is for orbit raising in a limited selection of orbits (such as a 12 o'clock sun-synchronous orbit). It is for this reason that Kennedy opted for a thermal-storage system for a fixed concentrator [1, 2004]. In past direct-gain concepts, all of which had thrust vectors parallel to the concentrator system optical axis [1,7,21-24], this problem was overcome by articulating the concentrator independently of the satellite platform and using off-axis parabolic mirror concentrators [7, 1989]. However, this adds complexity and mass to the concentrator subsystem. Generally direct-gain systems have
complicated optical/mechanical devices for solar tracking and collection but simple and light cavity receivers [14,1979].

Thermal-storage STP is not limited in power the same way as a direct-gain STP system. The ability to store energy allows much more power to be deliverable to the propellant for a single manoeuvre. However the size of the concentrator array does ultimately limit the final temperature to which the thermal-storage cavity receiver can approach. For micro-satellites thermal-storage can extend the range of micro-satellite applications with the ability to provide high thrust levels with competitive performance. Manoeuvres such as Geo-synchronous orbit insertion and Near-Earth Escape become viable for a micro-satellite and could be completed within a reasonable time [1, 2004]. Consider a 0.5 kg high heat capacity receiver with a specific heat \( C_p \) of 1,000 J/kg-K, heated to a temperature of 3,000 K. The power available to heat the propellant can be estimated for a 500 s burn time (\( \Delta t \)) through the following relation:

\[
P = M_r C_p \frac{\Delta T_r}{\Delta t} \quad \text{(3.2)}
\]

Where \( M_r \) is the mass of the receiver and \( \Delta T_r \) is the change in temperature of the receiver as heat is transferred to the propellant during a manoeuvre (assumed to be 2,000 K). This simple relation indicates a power of 2,000 W available to heat the propellant. This equates to an achievable thrust of 1.2 N, by the application of equation (3.1) assuming a “burn average” specific impulse of 350 s and an efficiency of 1.0 (ideal)\(^{12}\). It is necessary to refer to the “burn average” specific impulse as during the course of a manoeuvre the temperature of the thermal storage receiver will fall, due to convective heat transfer between the receiver and the propellant, causing a change in specific impulse as according to equation (2.1). The “burn average” specific impulse is the time average performance of the receiver during a manoeuvre. To thermally charge the thermal-storage cavity receiver the concentrator must be pointed on-Sun for a long period of time (>6000s), to attain a high receiver temperature. Once this is achieved the spacecraft can slew to the orientation required to thrust. Thus for a thermal-storage system there is no requirement to have an articulating concentrator array. However, during the slew manoeuvre the receiver will cool down, reducing receiver temperature. Typically thermal-storage systems have complex and heavy cavity receivers but simpler optical/mechanical devices for solar tracking and collection.

Both thermal-storage and direct-gain systems can benefit from augmentation by fibre optics. In addition to the system flexibility granted by fibre optics a mass advantage can also be attained through the use of multiple small concentrators.

\(^{12}\) This ideal efficiency is due to the concentrator system not taking part in the propellant heating during the burn.
3.1.2 Fibre Optic STP Options

With fibre optics a range of unique system variations and options can be realised. In this section the extent to how fibre optics can benefit an STP system on board a micro-satellite are explored. Firstly the features that enable these system variations and options are identified in figure (3.2):

In figure (3.2a) the depiction of the thermal storage STP system proposed by Kennedy and Palmer [30] is shown. This system does not use fibre optics, however, it is shown for the sake of comparison. As already discussed the concentrator for this system is rigid and fixed to one side of the spacecraft. For this system it is necessary for the sides of the spacecraft to be canted so that when the thermal storage receiver is being heated the spacecrafts solar panels can also be illuminated. Once the thermal storage receiver has reached its operating temperature, it is necessary for the spacecraft to orientate itself such that it can thrust in the direction required.

In figure (3.2b) the STP thruster/receiver has been mechanically decoupled from the concentrator and placed on another facet of the spacecraft. However, a fibre optic cable maintains the optical coupling between the two components. This is the first benefit of using fibre optics in a STP system. Although there is no obvious benefit for a thermal storage STP system, as the spacecraft will alter its attitude to change thrust directions, a direct gain STP system can benefit significantly. If the system shown in figure (3.2a) were a direct gain STP system then there would be only one thrust direction, towards the Sun. With the receiver placed on a side facet to the
concentrator facet then the thrust angle can be altered via rotation of the spacecraft about the optical axis (z) of the concentrator and also via rotation of the thruster itself. The selection of angle range can be decided based upon the orbit in which the satellite is being placed. This analysis is restricted to low Earth circular orbits. The Sun vector ($\hat{S}$) can be derived for any particular day of the year from:

$$
\hat{S} = \begin{bmatrix}
x \\
y \\
z
\end{bmatrix} = \begin{bmatrix}
\cos \alpha \cos^2 2\pi d + \sin^2 2\pi d \\
(1 - \cos \alpha) \sin 2\pi d \cos 2\pi d \\
- \sin \alpha \cos 2\pi d
\end{bmatrix} \quad \quad \text{(3.3)}
$$

where $\alpha$ is the angle at which the Earth is tilted to the ecliptic (23°) and $d$ is the fraction of the year for a particular day ($d = \text{day}/365.25$). Here winter solstice occurs when $d = 0$. This vector is given in a frame of reference centred on the Earth. In a rotationally displaced earth centred reference frame the along track vector ($\hat{A}$) of an orbit describing a spacecrafts motion can be calculated at a given true anomaly ($\theta$), measured from the point of equator crossing, for any circular low earth orbit via:

$$
\hat{A} = \begin{bmatrix}
\cos \theta \\
\sin \theta \\
0 \\
0 \\
0 \\
0
\end{bmatrix} \begin{bmatrix}
\cos \Omega \\
\sin \Omega \\
0 \\
0 \\
0 \\
0
\end{bmatrix} \begin{bmatrix}
\sin \Omega \\
- \cos \Omega \\
1 \\
0 \\
0 \\
- \sin \Omega \\
\cos \Omega
\end{bmatrix} \begin{bmatrix}
0 \\
0 \\
1 \\
0 \\
0 \\
0 \\
- \sin \Omega \\
\cos \Omega
\end{bmatrix} \begin{bmatrix}
1 \\
0 \\
0 \\
0 \\
0 \\
0
\end{bmatrix} \begin{bmatrix}
0 \\
0 \\
0 \\
0 \\
0 \\
0
\end{bmatrix} \quad \text{(3.4)}
$$

Where $\Omega$ is the right ascension of the ascending node (RAAN) of the orbit and $i$ is the inclination of the orbit. From (3.3) and (3.4) the angle between the along track vector and the Sun vector can be calculated for any circular orbit at any particular true anomaly and time of year. The drawbacks of this system are that the spacecraft would require a very accurate 3-axis controlled attitude control system to provide adequate sun vector pointing, and the use of the fibre optic for solar energy transmission will decrease efficiency.

In figure (3.2c) a further benefit of using fibre optics is seen. Here multiple concentrator mirrors are used in place of a single concentrator. This provides the advantage of lessening the mass of
the concentrator system as the mass of a rigid mirror is scaled by the fourth power of its diameter\(^13\) in order to maintain a certain level of surface quality (this is discussed further in chapter 4). In figure (3.4) it can be seen that the mass of the concentrator system drops rapidly as the number of concentrators for the same collection area are increased. The most significant benefit of this is the reduction in cost as the total mass of the satellite is decreased. The increased spacecraft area required to accommodate multiple concentrators offsets this benefit. The initial very significant drop in concentrator system mass suggests that it is now more feasible to allow even small numbers of multiple concentrators to be independently pointed at the Sun via standalone mechanisms. Subsequently less demand would be placed on the attitude system to obtain the required pointing accuracies and therefore less spacecraft subsystems will be impacted though the use of the system. Furthermore, a multiple concentrator system will be inherently redundant of single point failures and additional mirrors could be added to compensate for decreased efficiency through fibre optic transmission, with the system mass still remaining below the mass of a single concentrator mirror.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{figure3.4}
\caption{Solar concentration system mass vs. number of concentrator mirrors used in the concentration system. (Mirror material = Aluminium, required concentrator collection power = 245 W, required surface quality = 1\(\mu\)m, see chapter 4 section 4.2.4 for this calculation.)}
\end{figure}

In figure (3.2d) the final benefit of the use of fibre optics is visualised. Here a fibre optic optical switch box directs the solar energy from the concentrator system to multiple receiver/thrusters. In doing this further system options become available. For instance, a direct gain receiver and a thermal storage receiver could be powered from the same concentrator system, providing the STP

\(^{13}\) A conservative relation based on circular telescope mirrors [74, 1989]. Holota et al [75, 1999] employs a rule of thumb relation, for sizing of space mirrors, where the mass of the mirror is proportional to its diameter to the power 2.7. This is discussed further in chapter 4.
system with the benefits of both types of receiver. An optical switch box allows for the possibility of redundant solar receivers in case of receiver failure, which could be caused by repeated thermal cycling of the receiver to high temperatures. Also, multiple small direct gain receivers could be used to provide attitude control. However, the optical switch box would decrease system efficiency further.

### 3.1.3 Candidate Missions

We now consider a selection of missions that STP could enable micro-satellites to perform. The following analysis is restricted to the consideration of mini/micro-satellite platforms only as platform restrictions allow for a fair trade between other propulsion systems and the applicability of STP to small spacecraft is a key feature of this research. Kennedy [1, 2004] identifies three classes of missions applicable to a micro-satellite STP system, these are summarised in table (3.2):

![Figure 3.5: (a) DMC constellation design. (b) VPGS constellation [5,65]](image)

<table>
<thead>
<tr>
<th>Mission Class</th>
<th>Typical ΔV</th>
<th>Candidate Missions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Near-Escape</td>
<td>700 - 1,200</td>
<td>L2 orbiter &amp; Near Earth Object (NEO) missions</td>
</tr>
<tr>
<td>Geo-synchronous Earth Orbit (GEO)</td>
<td>-1,500</td>
<td>Micro-satellite GEO missions</td>
</tr>
<tr>
<td>Other Body Capture</td>
<td>1,500 - 4,000</td>
<td>Lunar and interplanetary missions</td>
</tr>
</tbody>
</table>

From table (3.2) we see that a relatively large ΔV is required for all mission classes when compared to conventional micro-satellite ΔV requirements, which are on the order of ~50m/s [5, 2003]. Current micro-satellite mission concepts are evolving to consider multiple satellite
configurations such as Earth observation constellations and synthetic aperture radar formations. A solar thermal formation flying propulsion system would be impractical as formation maintenance requires continuous propulsive response and STP requires a heat up period and cannot function in eclipse (particularly true for direct-gain systems). However an STP system for a micro-satellite constellation shows promise. Two high ΔV mission concepts involving small-satellite constellations are now discussed. Unlike the space missions analysed by Kennedy and Palmer [30, 2002] these missions begin in low earth orbit where consideration has to be given to eclipse periods.

**Micro-satellite Inspector (MI):** This mission concept is based on the Disaster Monitoring Constellation, a product of Surrey Satellite Technology Limited (SSTL) (figure (3.5a)). The current DMC constellation consists of 5 micro-satellites in a 10am sun-synchronous orbit at an altitude of 700 km. The DMC constellation is designed to achieve daily imaging of any part of the world for the purposes of global disaster monitoring [5, 2003]. The proposed mission takes the form of a micro-satellite inspector that regularly visits each member of the DMC micro-satellite for the purposes of inspection, possible maintenance and potentially acting as a temporary replacement if a situation occurs in which a constellation satellite is damaged or is being replaced. The inspector micro-satellite has a lifetime equal to the lifetime of the DMC constellation, this being 5 years. Normal operation of the inspector will involve it making visits to each satellite in the constellation every month. Nominally the inspector will take two weeks to transfer from one satellite to another and will spend a further two weeks supporting the current satellite before transferring to the next. The satellite must also be able to respond to emergency situations and be able to transfer to any satellite in the constellation within two days. A ΔV requirement is estimated at 1,250 m/s. The micro-satellite inspector’s burn out mass is assumed to be 100 kg and its dimensions are assumed to be 650 mm × 650mm × 680mm.

**High Latitude Earth Observation (HLEO) Mini-satellite Constellation:** This mission concept is based on the Virtual Polar Geo-stationary Satellite (VPGS) a concept studied at the NASA Jet Propulsion Laboratory [65, 2000] (figure (3.5b)). The concept consists of a constellation of 3 satellites in separate Molniya orbits spaced 120° apart longitudinally. A Molniya orbit is a highly elliptical orbit inclined at 63.43° and has an apogee at near 40,000 km altitude. These satellites are configured such that at least one satellite is functioning above the polar region at any one time. These satellites offer a large range of potential applications including:

- Monitoring of stratospheric ozone at high spatial resolution.
- Monitoring of polar tropospheric cloud systems.
- Prompt detection of high-latitude volcanic eruptions, with attention to airline safety.
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For this mission concept it is assumed that each satellite begins in a low earth orbit at the correct inclination and must transfer into the Molniya orbit under its own propulsion. A ΔV requirement is estimated at 2,300 m/s for insertion into a Molniya orbit, this is a typical value for an orbital manoeuvres into geo-synchronous transfer orbits [2, 1999]. The mini-satellite’s burn out mass is assumed to be 250 kg and its dimensions are assumed to be 1,500 mm × 1,500mm × 1,500mm.

3.1.4 First Order Propulsion System Trade

A first order comparison is now performed to establish the relative performance of STP systems with other competing systems. This analysis does not consider electric propulsion systems for the candidate missions proposed. This is due to the low amount of power available on mini/micro satellite platforms, (typically <100 W) and the subsequent long duration transfer times incurred for low power electric propulsion systems. Low molecular weight monopropellants were also not selected for consideration. Monopropellants such as hydrogen involve complex storage issues and require large storage vessels. The first order comparison considers theoretical performance only, for all propulsion systems and does not take into account nozzle expansion and thermochemistry inefficiencies. Kennedy conducted the same first order analysis for the mission classes noted in table (3.2) [1, 2004]. Table (3.3) lists candidate propulsion systems performance, as identified by Kennedy [1, 2004], and the required propellant mass to complete the DMC Inspector and HLEO Constellation missions:

<table>
<thead>
<tr>
<th>Propulsion System</th>
<th>Specific Impulse (s)</th>
<th>DMC Inspector: Prop Mass</th>
<th>HLEO Constellation: Prop Mass</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Satellite mass = 100 kg</td>
<td>Satellite mass = 250 kg</td>
</tr>
<tr>
<td>Ammonia-STP</td>
<td>407</td>
<td>36</td>
<td>195</td>
</tr>
<tr>
<td>Hydrazine-STP</td>
<td>407</td>
<td>36</td>
<td>195</td>
</tr>
<tr>
<td>Water-STP</td>
<td>333</td>
<td>46</td>
<td>255</td>
</tr>
<tr>
<td>N₂O₃/MMH</td>
<td>319</td>
<td>49</td>
<td>271</td>
</tr>
<tr>
<td>H₂O₂/kerosene</td>
<td>298</td>
<td>54</td>
<td>300</td>
</tr>
</tbody>
</table>

The values in table (3.3) were calculated using equation (2.2). As can be seen from table (3.3) there is a substantial difference in required propellant mass between STP and non-STP options. This is particularly apparent for the HLEO mission, which has the larger ΔV requirement. The mass saved for both missions could be utilised for mission enabling payloads, such as high-

14 The specific impulses in table (3.3) for the STP propellant options are based on a receiver temperature of 2,000 K as according to equation (2.5).
resolution cameras and high power antennas. Also affecting the overall mass of the propulsion system is the required storage volume for each propellant, as reported by Kennedy [1, 2004], this is summarised in table (3.4):

### Table 3.4 Candidate propulsion systems propellant storage volume requirement

<table>
<thead>
<tr>
<th>Propulsion System</th>
<th>Storage Density (kg/m³)</th>
<th>DMC Inspector: Propellant storage volume (Litres)</th>
<th>HLEO Constellation: Propellant storage volume (Litres)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ammonia-STP</td>
<td>600</td>
<td>60</td>
<td>325</td>
</tr>
<tr>
<td>Hydrazine-STP</td>
<td>1.005</td>
<td>36</td>
<td>194</td>
</tr>
<tr>
<td>Water-STP</td>
<td>1.000</td>
<td>46</td>
<td>255</td>
</tr>
<tr>
<td>N₂O₄/MMH</td>
<td>1200</td>
<td>41</td>
<td>226</td>
</tr>
<tr>
<td>H₂O₂/kerosene</td>
<td>1310</td>
<td>41</td>
<td>228</td>
</tr>
</tbody>
</table>

- Ammonia-STP, although providing one of the highest performances, suffers from the lowest storage density, resulting in large storage volume (21% of the micro-satellite inspectors volume and 10% of the HLEO mini-satellites volume). The reason for the high performance of ammonia propellant is because the ammonia molecule (NH₃) contains a high proportion of hydrogen. With ammonia decomposition in the solar receiver at high temperatures (2,000 K) the average molecular mass of the exhaust gas is reduced due to the high molar proportion of hydrogen, resulting in an increase system performance. Ammonia propulsion systems for small-spacecraft are pressure fed, vaporising liquid systems, where the ammonia is pressurised under its own vapour pressure which allows the ammonia propellant to be removed from the storage tank with out the requirement of a pressurant. Ammonia also has a lower freezing point than hydrazine, and does not require significant storage tank thermal conditioning. For these reasons ammonia storage and feed systems are much simpler and therefore more reliable that hydrazine monopropellant systems and bi-propellant systems [66, 2005][1, 2004][67, 1987].

- A monopropellant hydrazine STP system seems to offer the most competitive choice in terms of storage volume (13% of the micro-satellite inspectors volume and 6% of the HLEO mini-satellites volume) and performance. Like ammonia, the hydrazine molecule

---

15 For bi-propellant options the storage density represents the average density of the oxidiser and fuel combination and thus the storage volume also represents an average value. Please note that propellant storage volume is equal to propellant mass divided by propellant storage density.
(N₂H₄) contains a high proportion of hydrogen, resulting in its high performance. Furthermore, the hydrazine decomposition process is exothermal providing additional energy for propellant heating. Hydrazine monopropellant storage and feed system, although technically more complex than an ammonia system (requires pressurant), benefits from substantial space heritage [1, 2004][68, 2004]. However hydrazine is a relatively toxic substance and the subsequent costs of handling it are high.

- A water STP system offers relatively good performance at a low cost due to the propellants availability and ease of handling. The system storage volume occupies 16% of the micro-satellite inspector’s volume and 7.5% of the HLEO mini-satellites volume. However, water thrusters often experience complexities in propellant freezing, two-phase flow issues and thruster corrosion [69, 1998].

- The bi-propellant combination Nitrogen tetroxide (oxidiser, N₂O₄) and Monomethylhydrazine (fuel, MMH) are a hypergolic mix and has significant space heritage [2, 1999][68, 2004]. The system storage volume occupies 14% of the micro-satellite inspector’s volume and 6.7% of the HLEO mini-satellites volume. However hypergolic propellants are highly toxic, and thus due to handling issues are also costly. Furthermore bi-propellants propellant storage and feed systems are more complicated than mono-propellant systems.

- The bi-propellant combination Hydrogen Peroxide (oxidiser, H₂O₂) and kerosene (fuel) are not a hypergolic mix and although it has a reasonable storage volume it requires the highest propellant mass to achieve the required ΔV [1, 2004][70, 1998]. The system storage volume occupies 14.3% of the micro-satellite inspector’s volume and 7% of the HLEO mini-satellites volume. Furthermore, hydrogen peroxide is known to have long-term, spacecraft, storage issues in terms of tank material corrosion, and therefore exotic storage tank materials are required.

In the following sections a first order system design of a fibre augmented STP demonstration system is considered for flight on the UK-DMC micro-satellite. The propellant chosen for this system is ammonia owing to the benefits of its simple propellant feed system and relatively low toxicity in comparison to hydrazine. Furthermore, the University of Surrey has experience and infrastructure in using ammonia propellant for small-spacecraft propulsion system testing [1, 2004]; therefore ammonia propellant was considered the most expedient means of progressing the research project.
3.2 Detailed Mission Analysis

In this section the DMC inspector and HLEO missions are examined in detail. Essential astrodynamics necessary for the analysis of satellite orbital manoeuvres are reviewed. Then each mission is analysed via a Satellite Tool Kit (STK\textsuperscript{16}) analysis. These missions consider the use of solar thermal propulsion in Low Earth Orbit, which may have significant periods of eclipse, meaning that the power source to heat the propellant is not constant. However, in this analysis it is shown that with the use of fibre optics an STP system can be designed to overcome this disadvantage and provide competitive performances.

3.2.1 DMC Inspector

The orbital manoeuvres necessary for this mission are limited to only coplanar manoeuvres. Coplanar manoeuvres change an orbit's size, shape and argument of perigee but not other orbital parameters such as inclination or right ascension of ascending node. The orbital parameters of the DMC constellation are given in table (3.5):

\begin{table}[h]
\centering
\begin{tabular}{|c|c|}
\hline
Orbit Parameter & Value \\
\hline
Semi major axis & 7078.14 km \\
Inclination & 98° \\
RAAN & 167 \\
Eccentricity & -0.0002 \\
\hline
\end{tabular}
\caption{DMC orbit parameters}
\end{table}

The transfer time specified in the mission definition is around two weeks; it is therefore necessary to manoeuvre the inspector satellite into an orbit with an orbital period consistent with a two week drift from one DMC satellite to another. There are 5 DMC satellites spaced (more or less) evenly around the orbit, therefore after the two week period the inspector should have drifted through the 72 degrees separating each DMC satellite. Kepler’s third law gives the period \(T_o\) of a given orbit:

\[ T_o = 2\pi \sqrt{\frac{a^3}{\mu}} = \frac{2\pi}{\omega} \Rightarrow \omega = \sqrt{\frac{\mu}{a^3}} \quad \text{...(3.5)} \]

\textsuperscript{16} Satellite Tool Kit, a product of Analytical Graphics’, is a professional software tool for the analysis of orbital trajectories.
Where \( a \) is the semimajor axis of the orbit, \( \mu \) is the Earth's gravitational constant \( (\mu = 3.986 \times 10^{11} \text{ m}^3\text{s}^{-2}) \) and \( \omega \) (rads/s) is the average angular rate of the orbit. For the transfer orbit to be consistent with a drift of \( \theta = 72^\circ \) over the transfer period the angular rates should be differenced such that:

\[
\Delta \omega = \frac{\theta}{T_d} = \omega_{DMC} - \omega_r = \sqrt[3]{\frac{\mu}{a_{DMC}^3}} - \sqrt[3]{\frac{\mu}{a_r^3}} \quad (3.6)
\]

Where \( \Delta \omega \) is the difference in angular rates between the transfer and DMC orbit and \( T_d \) is the drift time. As seen in equation (3.5) the average angular rate of an orbit is directly related to the semimajor axis of the orbit. It is therefore possible to calculate the semimajor axis of the transfer orbit. The velocity \( (v) \) at any point in an orbit can be calculated from the vis-viva equation:

\[
v = \sqrt{\frac{2}{r} - \frac{1}{a}} \quad (3.7)
\]

Where \( r \) is the current distance of the satellite from the Earth's centre. From equation (3.7) the velocity change required to transfer from one orbit to another can be calculated. An effective way to move from one orbit to another is to employ a Hohmann transfer type maneuvers [2]. A Hohmann transfer maneuver is very efficient due to the propulsion system only operating when the along track vector of the spacecraft is very close to being perpendicular to the gravitational field vector, which therefore minimises the gravity loss incurred during the burn.

**Figure 3.6: Hohmann transfer maneuver [2]**

Figure (3.6) demonstrates what is involved in a Hohmann transfer maneuver. The initial orbit is the circular orbit of the DMC spacecraft. At a point in the orbit, just before the satellite enters the Earth's shadow, a near impulsive maneuver is performed, propelling the satellite into an elliptical transfer orbit. When the spacecraft arrives at the perigee of the elliptical orbit a second near
impulsive maneuver is performed, just after the satellite exits the Earth’s shadow, propelling the spacecraft into the necessary drift orbit.

\[
\Delta v_a = v_{trans_a} - v_{DMC} = \sqrt{\frac{\mu}{a_{DMC}}} \left( \frac{2}{a_{trans}} - 1 \right) - \sqrt{\frac{\mu}{a_{DMC}}} \quad \text{(3.8a)}
\]

\[
\Delta v_b = v_{Drift} - v_{trans_b} = \sqrt{\frac{\mu}{a_{Drift}}} - \sqrt{\frac{\mu}{a_{trans}}} \left( \frac{2}{a_{Drift}} - 1 \right) \quad \text{(3.8b)}
\]

Using equations (3.8a,b) the change in velocity of the spacecraft at a and b can be calculated to be -1.2 m/s for a 14 day transfer of 72° of drift, this equates to a 4.9 m/s velocity change needed to transfer from one satellite to another. For a transfer time of 2 days with a drift angle of 144° a 68.3 m/s velocity change is required. This therefore defines the velocity budget needed of the spacecraft so as to continue normal operations for a 5 year mission requires 325 m/s leaving 700 m/s available for 10 emergency maneuvers. A propulsion system for such a satellite should be capable of providing sufficient thrust to complete a maneuver within sufficient time. However, the above analysis makes the assumption of impulsive manoeuvres, such that at points a and b (in figure (3.6)) the spacecraft gains the change in velocity instantaneously. This assumption holds true, dependant on the amount of thrust the propulsion system is capable of providing and how long the thrust is maintained for. It is assumed that a 5 minute burn, corresponding to 5% of the DMC orbit, is sufficiently short for the impulsive assumption to hold true. The thrust estimated to be deliverable by a direct-gain STP system scaled to the size of a DMC satellite was approximately 0.1 N. A propulsion system providing this thrust for a 5 minute burn will deliver a \(\Delta V\) of 0.3 m/s, consuming 7g of propellant at a specific impulse of 400 s. Clearly this indicates that under such constraints the propulsion system is required to perform two Hohmann transfer manoeuvres to get into the necessary drift orbit and another two manoeuvres to return to the DMC orbit after a sufficient drift time. Although the time taken to get into the drift orbit is increased, as two Hohmann transfers are required, this is small compared to the 2 week drift time required. This is not the case for the emergency manoeuvre. In this case the required system “on” time is approximately one day. Therefore a Hohmann transfer manoeuvre can no longer be applied. The portion of the orbit in eclipse limits the “on” time of the propulsion system per orbit. Thus a longer duration burn must take place in the Sun-lit portion of the orbit. This leaves a low thrust spiral manoeuvre as the only option for the emergency manoeuvre. In this case the spacecraft will thrust for the entire Sunlit portion of the orbit, figure (3.7):
A low thrust spiral manoeuvre will have the effect of increasing the eccentricity of the orbit and its semi major axis, subsequently changing the orbital period necessary for the spacecraft to intercept the DMC spacecraft. However, the change in velocity required for the manoeuvre will increase, as a component of the thrust vector will be aligned with the gravity vector. This type of manoeuvre is difficult to analyse analytically, it is therefore examined using STK. As the DMC constellation is in a sun-synchronous orbit the Sun vector will be at a constant angle to the orbit's normal vector, which makes the analysis easier to deal with. Two STK scenarios were considered, one is with the inspector having to make an emergency manoeuvre of 72 degrees, this starts from, the Turkish contribution to the DMC constellation, the satellite BILSAT-1 and ends at the UK's contribution to the DMC constellation, the satellite UK-DMC. The second scenario is a 144 degree manoeuvre from BILSAT-1 to the Algerian contribution to the DMC constellation the satellite ALSAT-1 [5, 2003].

The results for each scenario are shown in table (3.6):
Table 3.6: DMC inspector STK results for emergency manoeuvre scenarios (700 m/s \( \Delta V \) available).

<table>
<thead>
<tr>
<th>Analysis Parameter</th>
<th>Scenario 1 (72° manoeuvre)</th>
<th>Scenario 2 (144° manoeuvre)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Required ( \Delta V ) (m/s)</td>
<td>64.4</td>
<td>96.6</td>
</tr>
<tr>
<td>Number of burns required</td>
<td>28</td>
<td>42</td>
</tr>
<tr>
<td>Time taken for manoeuvre (hours)</td>
<td>46.3</td>
<td>69.6</td>
</tr>
<tr>
<td>Propellant mass used (kg)</td>
<td>2.5</td>
<td>3.7</td>
</tr>
<tr>
<td>Eccentricity after manoeuvre</td>
<td>0.0008</td>
<td>0.0007</td>
</tr>
</tbody>
</table>

From table (3.6) it is evident that an emergency manoeuvre of 72° can be completed within the required two day time period however the 144° emergency manoeuvre requires an extra day to be completed. Therefore the thrust of the propulsion system should be increased to accommodate the required time limit. However, this will impact upon the solar concentrator collection area. It should be noted that with the available \( \Delta V \) capacity for emergency manoeuvres of 700 m/s that either ten scenario 1 manoeuvres or seven scenario 2 manoeuvres can be accommodated.

### 3.2.2 HLEO Constellation

In this mission the STP system is to act as a stage to transport the HLEO constellation satellites from a low earth orbit into a Molniya orbit. The reason for doing this is to reduce the cost of the launch of the constellation. The HLEO constellation when fully deployed will consist of three satellites in Molniya orbits, which have a RAAN separation of 120 degrees. This requires three separate launch vehicles to place each satellite in to an orbit with the correct RAAN and inclination, and then only coplanar manoeuvres are required of the STP stage. A launch vehicle delivering a satellite into a geo-synchronous transfer orbit (GTO) typically costs around $20,000 per kilogram of payload whereas delivering a satellite to low earth orbit (LEO) can cost as little as $5,000 per kilogram [2, 1999]. Furthermore if two of the satellites in the constellation possessed a stage such that they were capable of manoeuvring into their respective orbits from a single launch in to LEO, cost could be further reduced.

To begin with it is assumed that each satellite in the constellation starts in a low earth parking orbit of altitude 700 km with the correct inclination and RAAN. Orbits with this inclination do not have a constant angle between the orbit normal and the Sun vector, it is therefore assumed that the angle between the Sun vector and the orbit vector is 90°. This represents a worst case scenario as the portion of the orbit in eclipse will be at its largest. For near-circular orbits, eclipse duration \( (t) \) can be calculated from [52, 1992]:
\[ t = \cos^{-1} \left( \frac{1 - \frac{r_{eq}^2}{r_{sat}^2}}{\cos \beta} \right) \frac{T_o}{\pi} \] 

where, \( \beta \) represents the solar/orbit plane angle, \( T_o \) is the satellite's orbital period, and \( \frac{r_{eq}^2}{r_{sat}^2} \) is the ratio of the squares of equatorial planetary radius to satellite orbital radius. The system designed to deal with this constraint will be robust and the mission will not be restricted to be performed at particular times of the year. Furthermore, as this type of manoeuvre is consistent with a transfer into a geo-synchronous transfer (GTO) orbit, which will have periods of the orbit in eclipse, the analysis will be applicable to a STP stage for a GTO manoeuvre.

A direct gain system, although capable, is not suitable for this purpose due to the very low thrust with which it is associated (which subsequently requires an excessive amount of time to perform the manoeuvre. Instead a thermal-storage system is considered. The HLEO satellites are mini-satellites, with a dry mass of 250 kg. The STP propulsion system will operate at perigee in the lower latitude portion of the orbit in order to increase the altitude of the orbit apogee in the higher latitude portion of the orbit. Using equation (3.9), the eclipse period of a 700 km altitude circular orbit with a \( \beta \) angle of 90° is calculated to be approximately 36 minutes. It is assumed that the thermal-storage receivers for this concept experience significant cooling, via radiating energy into the space environment and conducting heat to the satellite platform, during the eclipse period. Therefore, heating of the receivers must be accomplished during the sunlit portion of a single orbit. Kennedy [1, 2004] discusses the ability of a thermal storage receiver to retain sufficient heat during eclipse portions of the orbits. Kennedy states that a thermal-storage receiver capable of retaining sufficient heat through eclipse periods is not unfeasible, however, the receiver would experience more severe thermal cycling, the effects of which could lead to receiver failure. This is investigated further in appendix B as well as thermal storage receiver sizing.

The thermal-storage concept for this mission takes the form of three thermal-storage receivers coupled to the concentrator system via fibre optics:

- **2-Newton Thermal-Storage Receiver**: The first of these receivers is small and is meant for use when the HLEO spacecraft is in LEO where the time available for heating between eclipses is small (~3,000 s).
- **6-Newton Thermal-Storage Receiver**: The second of these receivers is larger and is meant for use when the orbit apogee approaches medium Earth orbit (MEO) altitudes, such that more time is available for heating (~9,000 s). With more time available a larger receiver can be heated to high temperatures before getting to eclipse.
• 12-Newton Thermal-Storage Receiver: The third of these receivers is meant for use when the orbit apogee approaches geo-synchronous orbit altitudes (~18,000 s). This receiver is the largest as even more time is available to heat the receiver per orbit.

As in the previous section the STP system is assumed to have a specific impulse performance of 400 s. When at perigee the propulsion system will operate for a maximum of 1,200 seconds to ensure the resulting shape of the final orbit is correct and reduce incurred penalties for non-impulsive thrusting. Figure (3.9) shows the STK scenario of the HLEO constellation:

![Figure 3.9: (a) Final HLEO constellation. (b) Highlighted path of HLEO spacecraft transferring into Molniya orbit (red portion of the orbit indicates system operation).](image)

<table>
<thead>
<tr>
<th>Required ΔV (m/s)</th>
<th>2440</th>
</tr>
</thead>
<tbody>
<tr>
<td>Time taken for manoeuvre (hours)</td>
<td>402.6</td>
</tr>
<tr>
<td>Number of burns 2N</td>
<td>120</td>
</tr>
<tr>
<td>Number of burns 6N</td>
<td>40</td>
</tr>
<tr>
<td>Number of burns 12 N</td>
<td>12</td>
</tr>
<tr>
<td>Propellant used (kg)</td>
<td>210</td>
</tr>
</tbody>
</table>
Figure 3.10: Operation of HLEO STP system (red portion of the orbit indicates system operation).

In figure (3.10) the operation of the STP system can be observed. Upon exiting the eclipse of the Earth, the STP system begins to heat the thermal storage thruster. After a suitable heating duration the satellite should be approaching the perigee of the orbit where it can begin to thrust. During the manoeuvre the RAAN of the transfer orbits were observed to drift by ~23°. It was therefore necessary to displace the RAAN of the starting orbit such that upon completing the manoeuvre the satellite will have drifted into the correct RAAN. This is shown in figure (3.11):

Figure 3.11: HLEO constellation RAAN drift (red portion of the orbit indicates system operation).
Figure 3.12: HLEO constellation STP system receiver regions of transfer manoeuvre (red portion of the orbit indicates system operation).

Figure (3.12) depicts the portions of the manoeuvre for which each thruster is used. This analysis demonstrates how advantage can be taken of the high performance offered by solar thermal propulsion to enable small spacecraft to perform high ΔV manoeuvres. This analysis has also shown that it is feasible for a small satellite STP system to function within the constraints imposed by operating in LEO, due to the portion of the orbit in eclipse, via using fibre optics to make the system more versatile.

3.3 Solar Thermal Propulsion Demonstration Mission

Kennedy [1, 2004] suggests that a rapid approach to flight demonstrating a solar thermal propulsion system would be to design a modular fibre optic augmented STP system which could be placed on a host spacecraft as a technology demonstration experiment. Using fibre optics would allow multiple small concentrators to be placed in unobtrusive locations on the host satellite thus minimising the impact of having the STP demonstrator technology on board. Furthermore if a satellite’s attitude system were not capable of providing the Sun pointing accuracies required for efficient STP system operation, the small concentrators would have to be capable of independently tracking the Sun so as not to rely upon the satellites attitude system. In this section a first order design is described and analysed of a modular fibre augmented STP system, which would be suitable for flight on a number of suggested spacecraft.
3.3.1 Analysis of an STP Demonstrator System for the UK-DMC Spacecraft

In order to estimate the performance and potential configuration of a fibre augmented STP demonstration system, a conceptual system was designed for a micro-satellite subject to the requirements of its conventional propulsion system. The figures stated in the following analysis are estimates based on model data and literature research. The micro-satellite chosen for this study is the UK-DMC, a product of SSTL for the DMC international space consortium. Details of the UK-DMC propulsion system are given in table (3.8).

Table 3.8: DMC propulsion system details [5]

<table>
<thead>
<tr>
<th>Propellant</th>
<th>Butane (2.3 kg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specific impulse</td>
<td>77s – 85s</td>
</tr>
<tr>
<td>Thrust</td>
<td>20 mN (minimum)</td>
</tr>
<tr>
<td>Velocity capability</td>
<td>20 m/s</td>
</tr>
<tr>
<td>System wet mass</td>
<td>7.8 kg</td>
</tr>
</tbody>
</table>

The STP system is required to meet thrusting requirements and have at most the approximate system mass of the existing UK-DMC propulsion system. UK-DMC is in a 10.00am, sun-synchronous orbit at an altitude of 700km, and is a 3 axis stabilized nadir pointed platform. An STP demonstrator system cannot rely on the spacecraft to perform any sun-pointing manoeuvres, and should make use of a pointing mechanism with a large range of movement. Figure (3.13a) shows concept drawings of a single/multi concentrator fibre augmented STP demonstration system on the UK-DMC.

![Figure 3.13: (a) Multi concentrator arrangement on the UK-DMC space-facing facet. (b) Concentrator and pointing system unit concept.](image-url)
Fibre Optic Applications in Solar Thermal Propulsion Systems

An ammonia direct-gain propulsion system is selected due to its simplicity in design and its reliability. Fused silica fibre-optics are employed to channel energy from the concentrator units to the direct-gain cavity receiver. The concentrating units (figure (3.13b)) are considered modular and independent of each other, having built in control electronics. They are constructed completely from silicon carbide and the concentrating mirror has an aluminised highly reflective surface. The methods used to simulate the performance of this system are discussed in chapter 6. Table (3.9) details the properties of the designed fibre augmented STP demonstration system for the UK-DMC micro-satellite.

Table 3.9: Fibre augmented STP system details for the UK-DMC.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total system mass (kg)</td>
<td>7.85</td>
<td>Average over long duration burn. (Minimum)</td>
</tr>
<tr>
<td>Thrust (mN)</td>
<td>20</td>
<td></td>
</tr>
<tr>
<td>Optical system mass (kg)</td>
<td>3.6</td>
<td>6 concentrator units. Based on Kennedy [1, 2004] 300 g 10 cm diameter aluminium parabolic dish concentrators.</td>
</tr>
<tr>
<td>Fiber optics mass (kg)</td>
<td>0.5</td>
<td>6 silica fibre bundles. Estimate based on mass of 6 silica fibre optic cables with connectors.</td>
</tr>
<tr>
<td>Receiver system mass (kg)</td>
<td>0.3</td>
<td>Molybdenum receiver, graphite insulation, support structure, and propellant feed system. Kennedy [1, 2004]</td>
</tr>
<tr>
<td>Structure mass (kg)</td>
<td>0.75</td>
<td>Tank support structure. Standard UK-DMC propulsion system [71, 2004].</td>
</tr>
<tr>
<td>Propellant</td>
<td>Ammonia</td>
<td>Incorporates a simple storage/feed system.</td>
</tr>
<tr>
<td>Propellant tank volume (L)</td>
<td>2</td>
<td>Based on the propellant tanks of UK-DMC, which has two to store 2.3 kg of butane. Standard UK-DMC propulsion system [71, 2004].</td>
</tr>
<tr>
<td>Propellant tank mass (kg)</td>
<td>1.5</td>
<td></td>
</tr>
<tr>
<td>Propellant storage density (kg/m3)</td>
<td>600</td>
<td>See table (3.4)</td>
</tr>
<tr>
<td>Propellant mass kg</td>
<td>1.2</td>
<td>Tank volume × storage density.</td>
</tr>
<tr>
<td>Propellant storage pressure (bar)</td>
<td>8</td>
<td>Ammonia vapour pressure. Standard UK-DMC propulsion system [71, 2004].</td>
</tr>
<tr>
<td>AV capability (m/s)</td>
<td>31.5</td>
<td>Based on highest average Isp. Calculated via equation (2.2)</td>
</tr>
<tr>
<td>Specific impulse (s)</td>
<td>7.85</td>
<td>Average over 5 minute burn. See chapter 6.</td>
</tr>
</tbody>
</table>

The results reported in table (3.9) suggest that a fibre augmented STP demonstration system is capable of functioning within the design requirements of a micro-satellite and can feasibly provide performance competitive with conventional propulsion systems, for example hydrazine monopropellant thrusters. The STP propulsion system parameters in table (3.9) indicate an increased ΔV capability to 31.5 m/s compared to the UK-DMC butane resistojet propulsion system specifications, which could be used to extend the life of the satellite.
This STP demonstration system serves to demonstrate that STP could improve upon conventional propulsion systems but also represents an upper limit for the size of a STP demonstration system aboard the UK-DMC. With knowledge of the area and mass available for an STP experiment on a host satellite the true performance of the demonstration system could be derived. This relates directly to the number of independent concentrator units the host satellite could accommodate. In the following figure system performance is plotted against number of concentrators ($N$) in the system:

\[
I_{sp} = \frac{2\eta NI_a r^2}{Tg} \quad \ldots \ldots \text{(3.10)}
\]

In figure (3.13) the specific impulse ($I_{sp}$) is approximated from equation (3.10) and is for a STP system providing a continuous thrust ($T$) of 0.02 N. Here, $r$ is the radius of the individual concentrators (52.5 mm); $I$ is incident intensity (solar constant), $g$ is gravitational acceleration and $\eta$ is over all system efficiency ($\eta = 0.5$, representative of expected performance). This approach of using modular concentrator units provides system flexibility for potential flight opportunities. The limiting factor for potential flight opportunities is the available spacecraft area, mass and power. Therefore the concentrating units will be required to be as compact and light as possible, which is reflected in the above analysis. The system properties indicated in table (3.6) form the baseline concept for a fibre augmented STP demonstration system.

### 3.3.2 Candidate Micro-satellite Missions for Technology Demonstration

For the purposes of a technology demonstration of a STP system a number of future micro-satellite missions are now discussed and basic system requirements of a test demonstration are proposed. Future micro-satellite missions are listed in table (3.10):
Table 3.10 Candidate missions for technology demonstration of STP system [40,72,73]

<table>
<thead>
<tr>
<th>Programme</th>
<th>Status</th>
<th>Launch</th>
<th>Propulsion</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>PICARD</td>
<td>In Development</td>
<td>2008</td>
<td>Hydrazine</td>
<td>Solar observing satellite</td>
</tr>
<tr>
<td>MICROSCOPE</td>
<td>In Development</td>
<td>2008</td>
<td>FEEP</td>
<td>Equivalence Principle experiment</td>
</tr>
<tr>
<td>GEMINI</td>
<td>Concept Study</td>
<td>N/A</td>
<td>Hydrazine</td>
<td>Small satellite geo-stationary platform</td>
</tr>
</tbody>
</table>

The PICARD and MICROSCOPE missions identified in table (3.10) are particularly suitable for an STP demonstration mission. Both these satellites are members of the CNES MYRIADE series. They are both proposed to be in 6am-6pm polar sun-synchronous orbits and therefore, do not go into eclipse around the orbit. The GEMINI concept is under study at SSTL. GIOVE-A launched December 2005, demonstrated GEMINI core technologies [73, 2005].

PICARD: This platform is designed for solar observation and subsequently requires deployable solar panels and a high pointing accuracy of 36 arcseconds. The spacecraft weighs 150 kg, consumes 0.62 m³ of volume and can provide 42 W to the payload. This satellite would be an ideal platform for a STP demonstration. Deployable solar panels negate any shadowing issues that would otherwise occur for body-mounted panels. A reasonably large surface area would allow room for at least two small concentrators to provide concentrated solar energy to a small ammonia direct gain thruster. This not only would demonstrate STP augmented with fibre optics but also the use of multiple concentrators. The high pointing accuracy of the platform will assist a concentrator pointing system to acquire the Sun [40, 2005].

MICROSCOPE: This platform is designed primarily for an equivalence test experiment. This spacecraft weighs 200 kg, occupies 1m³ of volume, and can provide 200 W of power from its solar arrays. Like the PICARD platform deployable solar panels negate any shadowing issues
incurred by the host satellite. This platform having a much larger surface area than the PICARD platform offers the possibility of more concentrators to further demonstrate the multiple small mirror principle of a fibre augmented STP system and also increase achievable performance [72, 2005].

GEMINI: This platform is designed as a small satellite geo-stationary platform and again offers the advantages of a relatively large volume (1 m³) and deployable solar panels providing 800 W of power to the payload. As this satellite will be in a geo-stationary orbit, it will be in eclipse for certain periods of time. However, it is believed that through employing a direct gain ammonia system, rapid heat up of the thruster would resolve this issue [73, 2005].

![Figure 3.16: The GEMINI satellite platform [73]](image)

A fibre augmented STP system suitable for a technology demonstration mission aboard any of the above satellites would require at least two small concentrators coupled to a single thruster. The size of these small concentrators is dependant on the spacecraft area available. For example these concentrators could have a diameter of 110 mm. A suitable pointing mechanism would take up an area of 0.01 m², slightly larger than the area occupied by each concentrator. Thus the concentrators would take up a total area of 0.02 m². Such a mechanism would require approximately 0.5 W of power. The thruster would be located towards the edge of the spacecraft facet and would be coupled to the concentrators by fibre optic cables running through supported cables on the facet surface. The thruster, potentially reaching high temperatures, would be required to be thermally isolated from the structure to avoid compromising any sensitive instruments on the host satellite. The maximum temperature experienced by the thruster is dependant of the number of concentrators available.
3.4 Chapter Summary and Conclusions

3.4.1 Chapter Summary

Solar thermal propulsion is an enabling technology for micro-satellites to perform a greater variety of missions, which have large $\Delta V$ requirements (~2,000 m/s). In this section the benefits of fibre optic augmentation of a solar thermal propulsion system have been discussed. Candidate missions enabled for micro-satellites through solar thermal propulsion are identified. A case study of a fibre augmented solar thermal propulsion technology demonstration missions is performed for the UK-DMC micro-satellite. Finally candidate host satellites are identified that would be suitable for a solar thermal propulsion demonstrator mission.

3.4.2 Chapter Conclusions

- From the analysis and discussion in this chapter the following conclusions were made: A direct-gain small-spacecraft STP system has the most to gain from augmentation via fibre optics. With the mechanical separation of the concentrator and the receiver, via fibre optics, the use of a direct-gain STP system with a rigid, fixed concentrator can be considered for high $\Delta V$, LEO, small-spacecraft missions.

- The use of hydrazine propellant for a small-spacecraft STP system is the optimal choice from the considered propellant options. A hydrazine STP system provides the highest performance (~400 s specific impulse) and the lowest propellant storage volume (less than 10% of the spacecrafts total volume) out of the considered propellant options.

- The use of ammonia propellant was decided to be most appropriate for a STP demonstrator system. This is due to its high achievable performance (~400 s specific impulse) and simple propellant feed system in comparison to the other propellant options. Furthermore, the University of Surrey has experience and infrastructure in using ammonia propellant for small-spacecraft propulsion system testing [1, 2004]; therefore ammonia propellant was considered the most expedient means of progressing the research project.

- An ammonia STP system can conform to the mass, volume and minimum thrust requirements of the conventional propulsion system on board the UK-DMC micro-satellite. Furthermore, the STP can provide better performance and a subsequent increase in $\Delta V$ capability in comparison to the conventional propulsion system.

- Employing multiple concentrators and multiple receivers, provides system versatility and redundancy over conventional STP concepts. Furthermore, direct-gain and optical switching can be used to avoid inefficiencies due to significant receiver cooling during orbital eclipse periods.
Chapter 4

4 STP Concentrator Investigation

This chapter describes the work undertaken to investigate solar concentrator components suitable for an STP system and how concentrated solar energy can be efficiently coupled into a fibre optic cable. To facilitate this discussion, the mission requirements in the previous section are used to develop concentrator system components. Through consideration of individual components, design trades and analyses are identified and essential theory is discussed.

4.1 STP Demonstrator System Requirements

In chapter 3 it was stated that a STP system consisting of a number of modular concentrating units connected together by fibre optics would be an appropriate approach for rapid flight demonstration of solar thermal propulsion technology. For the technology to benefit from a flight demonstration then it is important for the system to demonstrate a certain level of efficiency and robustness. However, conflicting platform relative restrictions will also be imposed. Therefore two types of system requirements can be identified, namely performance requirements and platform requirements. The former concerns system performance parameters, such as specific impulse, thrust, overall energy delivery efficiency and system cost. The latter relates to how much platform mass, volume and power is available for a STP demonstration experiment. Consequently it is necessary to optimise system performance parameters whilst conforming to platform requirements. In this chapter the design of concentrator system components are considered which are designed to conform to the platform requirements of the UK-DMC micro-satellite, identified in chapter 3.

4.2 Concentrator Design

In this section the design issues concerning optical concentration systems are addressed. The various options for concentrator configurations and components are presented and the most appropriate options are selected based on system and platform requirements. Then component properties are derived.
4.2.1 System Configuration and Component Selection

The initial design choice for the concentrator system is a concentrator type and a concentrator pointing method. Table (4.1) lists the available options for concentrator type and table (4.2) lists concentrating pointing methods.

Table 4.1: Concentrator options.

<table>
<thead>
<tr>
<th>Concentrator Options</th>
<th>Description</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rigid Fixed</td>
<td>A concentrator manufactured from a solid material and located on the surface of the spacecraft.</td>
<td>Simple, cheap, reliable, highest areal density</td>
</tr>
<tr>
<td>Rigid Deployable</td>
<td>A concentrator manufactured from a solid material and located on a deployable surface external to the spacecraft.</td>
<td>Complex, expensive, reliable, light weight</td>
</tr>
<tr>
<td>Inflatable</td>
<td>A concentrator formed via inflating a membrane material deployed externally to the spacecraft.</td>
<td>Complex, unreliable, lowest areal density</td>
</tr>
</tbody>
</table>

The concentrator options presented in table (4.1) outline general characteristics for the types of concentrator available as described in the literature (see chapter 2). In light of these properties, a ‘Rigid Fixed” concentrator is selected for the micro-satellite fibre augmented STP demonstration system. The reason for this choice is not only due to the simplicity and low cost of the concentrator, but also because a rigid, fixed concentrator will impact the least upon a host satellite platform as no additional support structure is required.

Table 4.2: Concentrator pointing methods.

<table>
<thead>
<tr>
<th>Concentrator Pointing Method</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Non Articulated</td>
<td>Relies on satellite attitude control system, simple, limits performance and flexibility.</td>
</tr>
<tr>
<td>Articulating Mechanism</td>
<td>Complex, enhances performance and flexibility.</td>
</tr>
</tbody>
</table>

The concentrator pointing options presented in table (4.2) indicate a choice must be made between the use of the satellite’s attitude control system and the use of a mechanism. Relying on a satellite’s attitude control system to point the concentrator at the Sun is the simplest, most reliable option, but this competes with the satellites requirement to point its photovoltaic arrays at the Sun. For a thermal-storage STP system, which can potentially require Sun exposure for several hours, this is a significant problem. Solutions include deployable photovoltaic arrays, canted body panels and additional/larger batteries. In addition, a satellite’s attitude control system will have a very
limited capability to provide accurate pointing and compensate for spacecraft vibrations leading to pointing errors. Employing an articulating mechanism, although complex, allows the concentrator to track the Sun independently of the satellite’s attitude. The satellite’s attitude system, however, would be required to compensate for the motion of the concentrating mechanism, which could potentially weigh up to 15kg. This is especially the case for a single concentrator, which would require a large mechanism to orientate it and compensate for other spacecraft vibrations. The use of multiple smaller concentrators could reduce or even nullify attitude control system compensation. In addition the mechanism for each of the smaller concentrators would be lightweight, compact, and compensate for a higher level of spacecraft vibration thus making the pointing system more accurate. A multiple concentrator system also has inherent redundancy, for example, a single component failure may result in a single concentrator malfunctioning, but the system could still operate with the other concentrators available. This approach is selected for the fibre augmented STP demonstration system.

A number of optical components are available for the concentration of light. To avoid an over complicated concentrating optical system the number of concentrating components is limited to a single primary concentrator. Table (4.3) lists the available concentrating optical components.

Table 4.3: Rigid, fixed concentrator options [13]

<table>
<thead>
<tr>
<th>Concentrator</th>
<th>Ideal Concentration Ratio</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parabolic mirror</td>
<td>-13,000</td>
<td>Highest concentration, light weight, simple, expensive</td>
</tr>
<tr>
<td>Spherical mirror</td>
<td>-150</td>
<td>Lowest concentration, light weight, simple, cheap</td>
</tr>
<tr>
<td>Fresnel lenses or mirrors</td>
<td>-1,000</td>
<td>Heavy (lenses only), complex, expensive</td>
</tr>
</tbody>
</table>

From table (4.3) we can see the relative performance of the candidates for the primary concentrating component. Spherical mirrors are relatively simple to manufacture, and therefore, cheap, however, their concentration ratio is unacceptably low for this application. Fresnel lenses and mirrors although granting higher concentration with respect to spherical concentrators, does not outperform parabolic concentrators, which were chosen for this application.

4.2.2 Parabolic Concentrator Optics

A parabola is a curve that is defined as being the locus of points that are equidistant from a fixed line (the directrix) and a fixed point (the focus). This geometry is shown in figure (4.1). Thus, the
distance DR is always the same as FR [18, 1985]. If the origin of the parabola is taken to lie on the vertex (V), then the equation of the parabola becomes:

\[ y = 4fx^2 \quad \text{(4.1)} \]

where \( f \) is the focal length, the distance between the vertex (V) and the focus (F). When ray tracing an incident light ray parallel to the optical axis, as in figure (4.1a), twice the reflection angle (\( 2\psi \)) is always equal to the angle \( \psi \) through the laws of geometry. Light rays that are incident at any point on the mirror, therefore, will always be reflected towards the focal point, assuming they are incident parallel to the optical axis. For this reason, parabolas are ideal for the concentration of solar energy. For real parabolic surfaces of finite extent, the extremity of the mirror is defined by the rim angle (\( \psi_r \)), which is equal to twice the reflection angle at the mirror’s edge.

![Figure 4.1: (a) Parabolic geometry (b) Concentration ratio vs. rim angle for a parabolic concentrator](image)

In order to compute the dimensions and physical characteristics of a parabolic concentrator, the initial inputs required are the concentrator diameter (\( d_c \)) and the rim angle. The concentrator height (\( h \)) and focal length are then determined via the following relations [18, 1985]:

\[ h = \frac{d_c^2}{16f} \quad \text{(4.2)} \quad \tan(\psi_r / 2) = \frac{1}{4(f/d_c)} \quad \text{(4.3)} \]

Parabolic concentrators are, therefore, characterised by specifying a rim angle, a diameter and choosing a section of the paraboloid, whether it is on or off-axis.

A parabolic mirror has the unique property that light rays, of the same wavelength, incident parallel to the optical axis will be reflected to the same point on the optical axis. In practice, for sources of finite size at a finite distance, light rays will not be parallel to the optical axis and will form an image of the source at the focal point. This is further the case as reflection angles will
Fibre optic applications in solar thermal propulsion systems vary for different wavelengths of light. The ideal geometric concentration ratio is the ratio of concentrator area to image area and is subsequently related to the mirror rim angle, source angular size and mirror angular form error, a result of imperfect machining. Equation (4.4) relates the geometric concentration ratio to concentrator rim angle:

\[ CR_g = \frac{A_c}{A_f} = \frac{\sin^2(\psi_r)\cos^2(\psi_r + \theta_s \cos(\theta_r + \theta_f))}{\sin^2(\theta_s + \theta_f)} \ldots (4.4) \]

where \( \theta_s \) is the solar half angle and \( \theta_f \) is a measure of the mirrors angular form error. Plotting equation (4.4) we can see that the maximum concentration ratio is achieved for a rim angle of 45°, (see figure (4.1b)).

### 4.2.3 Fibre Optic Coupling

For a parabolic concentrator to direct light into a fibre optic, the fibre must lie along the optical axis with the fibre tip at the focal point as shown in figure (4.2).

\[ d_f = \frac{d_c \sin(\theta_s + \theta_f)}{\sin(\psi_r)\cos(\theta_s + \psi_r + \theta_f)} \ldots (4.5) \]

Thus, the concentrator diameter is restricted by the fibre’s diameter. To couple a single fibre optic to a concentrator such that all light rays incident from the mirror propagate along the fibre optic requires that:

- The mirror is accurately pointed at the source.
- The image size is the same as the fibre tip size, thus constraining the mirror size (Equation 4.5).
- The light rays are incident at an angle less than the maximum acceptance angle of the fibre (NA), thus constraining the mirror rim angle.
This means the design of a concentrator is heavily dependant upon the fibre optic selected. The data presented in figure (4.3a) is from a study conducted at the Department of Solar Energy and Environment Physics of the Ben-Gurion University of the Negev. The study was designed to determine the performance of a solar concentrator focusing light into a fibre optic. The data shows the transmission of light through a fibre optic for various angles of incidence [55, 2002].

![Figure 4.3: (a) Transmission vs. incidence angle for a fused silica fibre optic [55] (b) Restriction of rim angle due to fibre acceptance angle.](image)

The transmission through the fibre is good (100%-80%) up to the acceptance angle of the fibre ($\psi_s = 41.3^\circ$). A large decrease in transmission is then observed with increasing incidence angle. Figure (4.3b) demonstrates that a concentrator with a rim angle larger than the acceptance angle of the fibre optic will lead to light from the periphery of the mirror being significantly impeded upon transmission through the fibre optic. Implying the periphery of the mirror is somewhat redundant. In the context of spacecraft, for which weight is a precious commodity, this would be unacceptable. By reducing the rim angle of the concentrator mirror to the acceptance angle of the fibre optic, the mirror is optimised.

### 4.2.4 Practical Considerations

A consequence of the concept of multiple parabolic concentrating mirrors is a significant reduction in optical system mass when compared to that of a single mirror system for a given collective power output. Ashby and Jones [74, 1980] employs a conservative relation for the mass of a rigid space mirror, with the mass of the mirror a function of the $4^{th}$ power of the mirror diameter. This relation allows for a maximum tolerated deflection in a gravity environment such that the surface quality of the mirror can be ensured during manufacture. This also means that surface deformation due to release from gravity will be negligible. Ashby’s equation relating mirror mass ($M$) to mirror diameter ($d$) is given as:
Fibre Optic Applications in Solar Thermal Propulsion Systems

\[ M = \left( \frac{3g}{4\delta} \right)^{1/2} \pi \left( \frac{d}{2} \right)^2 \left( \frac{\text{den}^3}{E} \right)^{1/2} \] \hspace{1cm} \ldots (4.6) [74]

where \( g \) is acceleration due to gravity, \( \text{den} \) is the material density, \( E \) is the material's Young's modulus and \( \delta \) is maximum tolerated deflection of the mirror surface due to sag under its own weight [74, 1980]. If the area of a single mirror, satisfying a given power requirement \((P)\), is distributed among \( N \) smaller mirrors, then the mass of each mirror can be calculated via:

\[ M = \frac{1}{N} \left( \frac{3g}{4\delta} \right)^{1/2} \left( \frac{\text{den}^3}{E} \right)^{1/2} \left( \frac{P}{NI} \right)^{1/2} \] \hspace{1cm} \ldots (4.7)

Where \( I \) is the incident intensity (solar constant 1353 W/m²). Figure (4.4) demonstrates the trend of the reduction in mirror mass for a number of common mirror materials, assuming the Ashby and Jones relation:

Figure 4.4: (a) Mass of individual concentrator mirror vs. number of mirrors in concentrator system, Ashby and Jones relation [74, 1980]. (b): Over all mass of concentrator system vs. number of mirrors in concentrator system, Ashby and Jones relation [74, 1980].
Figure (4.4a) demonstrates the reduction of individual mirror mass with increasing the number of mirrors for a given power/area requirement. For this particular case a power requirement of 250 W was specified with a maximum tolerable deflection of $1 \times 10^{-6}$ m. Figure (4.4b) shows the reduction in the overall concentrator system mass with increasing number of mirrors. From figure (4.4b) it is apparent that increasing the number of mirrors in the system reduces the difference in mass between concentrator systems of the different materials. This relation therefore implies that with multiple mirrors, cheaper, readily available and easily manufactured materials can be considered, such as aluminium. The Ashby relation is also consistent with the mass of a single aluminium mirror, designed and manufactured by Kennedy [1, 2004] to collect 250 W of solar power.

Holota et al [75, 1998] employ a rule of thumb relation for the mass of a light weighted space mirror, based on past space mirror designs in the literature. This relation is given by:

$$M = \frac{1.2 \times 10^4}{\sqrt{\delta}} \left( \frac{\text{den}}{E} \right) d^{2.7} \quad (4.8)$$

In the Holota et al relation, the maximum tolerable deflection ($\delta$) is in nm, mirror density ($\text{den}$) is in g/cm$^3$, mirror Young’s modulus ($E$) is in GPa, mirror mass ($M$) is in kg and mirror diameter ($d$) is in m. If the area of a single mirror, satisfying a given power requirement ($P$), is distributed among $N$ smaller mirrors, then the mass of each mirror can be calculated via:

$$M = \frac{1.2 \times 10^4}{\sqrt{\delta}} \left( \frac{\text{den}}{E} \right) \left( \frac{4P}{\pi N} \right)^{2.7} \quad (4.9)$$

Figure (4.5) demonstrates the trend of the reduction in concentrator system mass for a number of common mirror materials, assuming the Holota et al relation. Figure (4.5a) demonstrates the reduction of individual mirror mass, by increasing the number of mirrors for a given power/area requirement. For this particular case, a power requirement of 250 W was specified with a maximum tolerable deflection of $1 \times 10^{-6}$ m, which is the same as Ashby and Jones relation case considered previously. Figure (4.5b) shows the reduction in the overall concentrator system mass with increasing number of mirrors. The relation of Holota et al, like the Ashby and Jones relation, demonstrates a trend to lower total concentrator system mass by increasing the number of mirrors in the system, although the effect is not as pronounced in comparison to the Ashby and Jones case. The Holota et al relation is not found to be consistent with the mass of a single aluminium mirror designed and manufactured by Kennedy [1, 2004] to collect 250 W of solar power. The Holota et al relation indicates that there is a significant mass saving in using exotic, expensive mirror materials such as silicon carbide in comparison to more common, cheaper mirror materials.
such as aluminium\textsuperscript{17} [1, 2004][76, 2004]. For silicon carbide it can be seen in figure (4.5b) that employing multiple mirrors does not grant much significant mass savings. However, for aluminium it can be seen in figure (4.5b) that employing multiple mirrors does grant significant mass savings, for example, a 33% mass saving is observed between employing a single mirror and a concentrator system consisting of 3 mirrors but only a further 9% mass saving (42% overall is observed for a concentrator system consisting of 6 mirrors.

Figure 4.5 : (a) Mass of individual concentrator mirror vs. number of mirrors in concentrator system, Holota et al relation [75, 1998]. (b): Overall mass of concentrator system vs. number of mirrors in concentrator system, Holota et al relation [75, 1998].

The three materials shown in figure (4.4) are taken from a selection of materials typically used for spacecraft mirrors:

\textsuperscript{17} Kennedy [1, 2004] designed and manufactured a 0.56m diameter aluminium parabolic dish concentrator mirror with a diamond turned surface. The cost of this concentrator was quoted to be $2,500. Kennedy decided to use aluminium in comparison to advanced ceramic material options due to the high fabrication cost of these and immature fabrication processes of these advanced materials.
Table 4.4: Typical Space Mirror Materials. [77]

<table>
<thead>
<tr>
<th>Material</th>
<th>Coefficient of thermal expansion (μm m⁻¹ K⁻¹ @ 0 K)</th>
<th>Density (kg m⁻³)</th>
<th>Young’s Modulus (GPa)</th>
<th>Thermal Conductivity (W m⁻¹ K⁻¹ @ 0 K)</th>
<th>Specific Heat Capacity (J kg⁻¹ K⁻¹)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Aluminium</td>
<td>23.9</td>
<td>2710</td>
<td>70</td>
<td>237</td>
<td>900</td>
</tr>
<tr>
<td>Beryllium</td>
<td>11.5</td>
<td>1848</td>
<td>287</td>
<td>218</td>
<td>1880</td>
</tr>
<tr>
<td>Fused Silica</td>
<td>0.55</td>
<td>3110</td>
<td>73</td>
<td>1.38</td>
<td>740</td>
</tr>
<tr>
<td>Silicon Carbide</td>
<td>2.2</td>
<td>3220</td>
<td>410</td>
<td>77.5</td>
<td>700</td>
</tr>
<tr>
<td>ULE</td>
<td>0.03</td>
<td>2210</td>
<td>67</td>
<td>1.3</td>
<td>708</td>
</tr>
<tr>
<td>ZERODUR</td>
<td>0.05</td>
<td>2530</td>
<td>90</td>
<td>1.64</td>
<td>820</td>
</tr>
</tbody>
</table>

The properties desirable of materials suitable for spacecraft mirror applications include:

- High stiffness (High Young’s Modulus).
- Lightweight (low density).
- Low Coefficient of Thermal Expansion (CTE).
- High thermal conductivity.
- High specific heat capacity.
- Low cost.
- Easy to machine and handle.

A high stiffness low weight material is necessary to provide good structural integrity during launch and in orbit as well as minimising spacecraft weight. A low CTE coupled with a high thermal conductivity and specific heat capacity increases the materials resistance to thermal shock and cycling in terms of maintaining mirror shape and surface quality.

There are techniques available, referred to as “Lightweighting” to further reduce mirror mass by factors of 5 or more. Only the top reflecting surface of a mirror is required for it to function, thus material can be removed from the rear of the mirror substrate in such a way as to produce a mirror of smaller thickness with a web stiffened structure, as shown in figure (4.6) [78, 2003][1, 2004]

Figure 4.6: (a) Front surface of typical parabolic concentrator. (b) Rear surface of parabolic concentrator with light weighted web stiffened structure. [1]
When lightweighting space mirrors extreme care should be taken to ensure deformations of the top reflecting surface between webs are at a minimum especially during polishing. If a glass is used as the mirror substrate dynamic low frequency vibrations (substrate motion, tautology) can lead to surface deformations, due to the low stiffness of the glass.

Aluminium is a common material used for terrestrial applications. Easy to machine, handle and is particularly cheap. When polished yields a high broadband reflectivity. Although having a good specific stiffness, it is limited to small optics for space applications, due to its high CTE.

Beryllium is a metal used extensively for cryogenic applications due to its near zero CTE at cryogenic temperatures (<70 K). However, for room temperature applications it suffers from a high CTE in the same way aluminium does. Beryllium is also high cost, difficult to handle due to its toxicity and difficult to machine. Despite this it has a high specific stiffness due to its low density and high modulus, making it very attractive for very lightweight mirrors.

Fused Silica is a glass, typically the material of choice for large optics operating at room temperature. Although it has a low CTE at room temperature its high density and low modulus are significant disadvantages. Furthermore, glass materials tend to be high cost and are difficult to handle.

Silicon Carbide is a ceramic material only recently applied to space mirror applications. It offers a very high stiffness coupled with an acceptable combination of the other key mirror properties. There are several forms of silicon carbide available with varying properties. Chemical vapour deposited (CVD) silicon carbide can be polished to a high surface finish of <1nm RMS, however the manufacturing process is expensive.

Ultra Low Expansion (ULE) titanium silicate is a glass material, which has been tailored to have a very low CTE. Other properties are comparable to those of fused silica, however its low CTE makes it more expensive. This particular material was used to construct the primary mirror of the Hubble Space Telescope, which is 2.4 m in diameter and weighs 826 kg.

ZERODUR, like ULE, is a glass material tailored to have a very low CTE. Glasses such as ZERODUR and ULE require an exceptional processing control environment in order to attain their very low CTE values, subsequently manufactures only make the material available as large blanks due to the length of time required to produce them. Once the blank is produced the material must then be polished, which also takes a long time. Mirrors manufactured from these materials are therefore very expensive. [77, 2000][78, 2003][79, 1998][80, 1997][81, 2001][1, 2004].

Apart from aluminium, mirror substrates made from these materials require a coating to be applied to their polished surfaces in order to give the mirror the desired reflective properties. These coatings are typically metals such as aluminium, silver and gold. For a space application these coatings should be highly adherent, free of pinholes and be themselves resistant to a space
environment. In figure (4.7) the specular reflectance spectrum of a number of coating options are plotted:

![Graph showing spectral reflectance of candidate mirror surface materials.](image)

Figure 4.7: Spectral reflectance of candidate mirror surface materials. [1]

There are a number of practical concerns regarding optical surface manufacture that should be addressed when considering the concentrator mirror. Several terms refer to the quality of an optical component:

- **Peak-to-Valley**: This value refers to the sum of the largest negative deviation to the largest positive deviation of the actual surface from the ideal surface.

- **Root-Mean-Square (RMS) Surface Form Error**: This value refers to the deviation of the actual surface from the perfect surface (or wave front) averaged over the actual surface. For example, for optical quality mirrors, the RMS surface form error should have a value of no greater than \( \lambda/10 \) (where \( \lambda \) is the wavelength of interest) [1, 2004].

- **Strehl Ratio**: This is a measure of the mirrors ability to focus light within its ideal image size and is a direct measure of the reduction in intensity due to surface quality.

To achieve a good quality surface, a suitable machining method is required, such as diamond turning suitable for the manufacture of aluminium mirrors. Although solar concentrators are not imaging optical components it is necessary for them to have near optical quality surfaces to ensure a high concentration ratio is obtainable. Thermal expansion and contraction of a parabolic concentrator mirror would result in the focal point shifting back and forth around the optical axis which could cause a misalignment with a fibre optic. To account for thermal expansion of the concentrator, the supporting structure would ideally be athermal with the concentrator. However,
consider a 300 g aluminium mirror, 105 mm in diameter. It would absorb 0.5 W of solar energy (assuming 0.95 reflectivity from a diamond turned surface) that would result in an 8 K rise in temperature over an hour heating period in vacuum. Given the CTE of aluminium shown in table (4.4) this translates into a 2 µm/m distortion of the mirror which is negligible, seeing as the characteristic dimension of such a mirror is much smaller than a metre. What is of more concern thermally is when a flat secondary (relay) mirror is employed to create a Cassegrain style concentrating system as shown in figure (4.8a).

![Image of a compact Cassegrain concentrator assembly](image)

Figure 4.8: (a) Compact Cassegrain concentrator assembly. 
(b) Heat up of a silver coated, BK7 substrate, secondary relay mirror.

The benefit of such a system is evident in that the fibre can be easily supported and aligned along the focal axis. The smaller secondary mirror, however, is subject to a much higher intensity of light and could consequently reach a much higher temperature. Figure (4.8b) shows the vacuum heating curve for an 18 mm diameter, silver coated, BK7 glass substrate, secondary mirror subject to heating from a 105 mm diameter aluminium concentrator, optimised for a 1mm diameter fibre. Figure (4.8b) shows the secondary mirror approaching 400 K after an hour of vacuum heating. At this temperature, the mirror would have expanded by 0.1 mm in diameter and 0.02 mm in depth. The most critical of these expansions is the latter as this is along the focal axis and would cause a growth of the solar image on the fibre tip, resulting in a loss of power. In this case, however, the alignment tolerance could afford to be much greater than this expansion. For example, if a power tolerance of ± 0.2 W were acceptable, the alignment tolerance would be ± 0.5 mm along the optical axis. Thus, provided that at this temperature, the optical properties of the silver surface were not compromised, this type of secondary mirror would offer a suitable solution.
4.3 Detailed Concentrator Analysis

In this section a detailed analysis of the concentrator optical system is presented for the STP UK-DMC demonstrator system. Initially the key features of the concentrator for this system are reiterated along with the theoretical performance (i.e. concentration ratio and spot size). Then a matlab model is presented which performs a ray tracing procedure to assess the size and shape of the solar image for the cases of an imperfect surface and with the presence of a Sun-vector pointing error. This model is confirmed using ZEMAX, which is also used to determine the intensity distribution of the concentrator solar image. Finally the design of the concentrator to be manufactured is presented and discussed.

4.3.1 Basic Design of Concentrator

The key design parameters of a concentrator optimised for coupling light to given fibre optic were discussed in section 4.2 For the UK-DMC STP demonstration system, these parameters are provided in table (4.5).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Concentrator diameter</td>
<td>105 mm</td>
<td>Small enough to be efficiently coupled to a 1 mm fibre optic.</td>
</tr>
<tr>
<td>Solar Image/Fibre Optic diameter</td>
<td>1 mm</td>
<td></td>
</tr>
<tr>
<td>Concentrator rim angle</td>
<td>40°</td>
<td>Appropriate for a fibre numerical aperture of 0.66</td>
</tr>
</tbody>
</table>

4.3.2 Ray Trace Analysis of Concentrator

The purpose of performing a ray trace analysis of the concentrator was to assess the behaviour of the concentrator solar image (referred to as the spot) with variations in concentrator surface quality and focal plane alignment. This was done in order to specify the required surface quality required for manufacture of the concentrator and provide tolerance values for the concentrator support structure.

Initially a ray-tracing algorithm for a 3-dimensional concentrator mirror was created in matlab\(^\text{18}\). The algorithm assumed a single parabolic reflecting surface and monochromatic rays incident at angles consistent with a solar source. Two solar disk intensity models were used for the ray trace simulation, namely the square model and the uniform model shown in figure (4.9).

\(^{18}\) Matlab is a mathematical computer simulation tool, a product of MathWorks Ltd.
The square solar disk model is simply that whatever angle at which a ray is incident the observed intensity is the same. The uniform model considers the surface of the Sun to be lambertian and therefore to obey Lambert’s cosine law\textsuperscript{19}. Subsequently the solar disk angular intensity distribution (as seen in figure (4.9a)) is given by:

\[ I = I_o \cos \frac{\psi}{\theta} \]  

(4.10)

Where \( I \) is the observed intensity, \( I_o \) is the maximum intensity observed from the Sun surface and \( \psi \), as defined in figure (4.9b), is given by:

\[ \psi = \pi - \arcsin((d/r) \sin \theta) \]  

(4.11)

Where \( d \) is the distance between the observer and the Sun (1 Astronomical Unit (AU)), \( r \) is the Sun’s radius and \( \theta \) is the observing angle (see figure (4.9b)). These solar disk models agree with

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\textsuperscript{19} Lambert’s cosine law states that the reflected or transmitted luminous intensity in any direction from an element of a perfectly diffusing surface varies as the cosine of the angle between that direction and the normal vector of the surface. As a consequence, the luminance of that surface is the same regardless of the viewing angle.[71]
those employed for a similar analysis of cylindrical, parabolic concentrators conducted by Nicolás [44, 1987]. According to Nicolás the uniform solar disk model is representative of the real, experimentally measured, solar disk which has the included effect of limb darkening (see figure (2.14)).

In the simulation, rays are generated with vectors which are consistent with the solar disk models used. Once a ray has been generated the intersection point on the surface of the concentrator is computed. The ray reflection vector ($\hat{R}$) from the concentrator surface is computed from the incident vector ($\hat{i}$) and the surface normal vector ($\hat{n}$) via:

$$\hat{R} = \hat{i} - 2(\hat{i} \cdot \hat{n})\hat{n}$$

Figure 4.10: (a) Ray intersection for square solar disk model. (b) Estimated spot intensity profile for square solar disk model. (c) Ray intersection for uniform solar disk model. (d) Estimated spot intensity profile for uniform solar disk model.
With the ray reflection vector the intersection of the ray with the focal plane of the concentrator is then calculated. The reflection in equation (4.12) does not take into account any spectral dependency; this is because mirrors do not suffer from chromatic aberrations [82, 1992].

Figure (4.10) shows the output of the simulation for both Sun disk models using a simulated concentrator mirror with a perfect parabolic surface. Figures (4.10a&c) show the cluster of ray intersection points, which represent the scale of the spot, on the focal plane of the concentrator for the square and uniform Sun disk models respectively. Figures (4.10b&d) shows the estimated variation in spot intensity with distance from the focal axis (calculated from ray intersection frequency with spot radius).

As expected the estimated intensity profile of the spot takes the form of the solar disk model used for the ray trace simulation in both cases [44, 1987]. Integration of these profiles to obtain the power contained within the spot adds to their validity, as this matches the reflected power of the concentrator. Furthermore Nicolas shows very similar intensity distributions for cylindrical parabolic concentrators. In order to confirm the validity and accuracy of the ray trace models result, a professional optics software package (ZEMAX) was employed to perform the same analysis as the one above. In figure (4.11) the ZEMAX intensity distribution of the spot is plotted with a smoothed version of the matlab ray trace model intensity distribution. Both plots are normalised with respect to the ZEMAX intensity distribution. Figure (4.11) shows good agreement between the two intensity distributions with a 95% correlation. This analysis served to provide confidence in the matlab ray trace model such that further analysis could be conducted with it.

![Intensity distribution of ray-traced concentrator spot.](image)

The circles around the cluster of intersection points in figures (4.10a&c) is the theoretical spot size for the concentrator as calculated from equation (4.5). It is of interest to determine how the spot size changes with varying surface quality. In section 4.2.4 the RMS surface form error was
defined as a measure of surface quality. The surface of the concentrator in the simulation can be
generated to have an RMS surface form error of any given value. This is done in the simulation by
adding a randomly generated error function to the perfect parabolic surface of the concentrator.
The error function is generated to have standard deviation equal to the RMS surface form error.
This error function perturbs the concentrator surface at a series of discrete points, the surface
slope between these points is calculated to provide surface normal vectors at ray intersection
points. The error function generated is dependent on concentrator radius only and is therefore
axially symmetric about the focal axis of the concentrator, see figure (4.12a).

Figure 4.12: (a) Representation of the concentrator parabolic surface with added RMS surface form
error. (b) Geometric concentration ratio of concentrator vs. concentrator RMS surface form error.

Figure (4.12b) shows how the geometric concentration ratio, defined in section 4.2, decreases
with increasing RMS surface form error according to the simulated ray trace model of the
concentrator. It is clear that the concentration ratio is very sensitive to variation of the RMS
surface form error and that only a surface with a high quality (and therefore very low RMS
surface form error value) will provide the desired concentration ratio. From figure (4.12b) it is
found that only a surface with an RMS surface form error value of less than 0.1 μm will provide
acceptable concentration for the concentrator surface to be manufactured.

Optical switching of the concentrated solar energy from a parabolic mirror can be achieved by
purposefully pointing the concentrator to align with different fibres within a fibre bundle which
lie on the focal plane (see figure (7.12)). As the fibres are no longer located at the focal point,
rather slightly to one side, there is a power loss resulting from a change in the shape and size of
the spot on the focal plane (this effect is known as coma). Using the ray trace model of the
parabolic concentrator mirror this effect was considered. From figure (7.12) it is clear that the spot
is required to be displaced from the focal point by a distance just larger than the fibre optic radius
as allowances must be made for the fibre cladding and buffer (making the diameter of the fibre optic 1.3mm) as seen in figure (4.13). The spot in figure (4.13) is represented again by a cluster of ray intersection points on the focal plane of the simulated concentrator. For the case in figure (4.13) the concentrator focal axis has been misaligned with the Sun vector by 0.4° such that most ray intersection points lie within the red circle which represents a displaced fibre optic. The resulting spot exhibits the effects of coma such that the spot is more elongated in the direction of spot displacement and is slightly larger than the fibre optic resulting in a loss of concentration.

Upon integrating the intensity profile of the spot over the tip of the bundle a 4% power loss is observed due to the drop in spot concentration. This value could be made smaller via reducing the spacing between the fibres in the bundle. A 4% power loss is consistent with conventional micro mechanical optical switch losses reported in the literature [57, 2007]. This means of performing optical switching is discussed further in chapter 7.

### 4.3.3 Design of Manufactured Concentrator

For this research it was decided that a number of the small concentrators suitable for the UK-DMC STP demonstration system should be manufactured for ground testing purposes. It was therefore necessary to construct a detailed optical and mechanical design of the concentrator and support structure. It was decided that a cassegrain type system was most suitable in order to facilitate the alignment of the fibre bundle with the focal point of the concentrator. However, it was important to identify a tolerance for this alignment. This was found from the matlab ray trace simulation of the concentrator.

In figure (4.14) the radius of the spot is measured at several locations on the focal axis around the focal point of the concentrator. In order to maintain a small spot radius it can be seen in figure
(4.14) that a tolerance of less than ±0.1 mm is required to provide a spot radius of less than 0.55 mm (giving a minimum concentration of 10,000). An equally tight tolerance is required for the lateral alignment of the fibre also. From the above analysis it is apparent that unsuitable alignment of the fibre with the concentrator spot will be a massive source of power loss due to the tight tolerances involved.

The mechanical design of the cassegrain concentrator unit was performed using SolidEdge a computer aided design software package. The final design model of the cassegrain concentrator unit is shown in figure (4.15). Key design points to note from figure (4.15) is that the parabolic concentrator mirror is only in contact with the supporting back plate behind the concentrator which minimises any distortion of the mirror surface during high vibration conditions. In fact all structural support stems from the back plate, that also acts as the adapter for the pointing mechanism. This includes support for the secondary relay mirror. The concentrator is suitably thick to be able to support its surface with negligible surface sag as according to equation (4.6).
Furthermore it should be noted, in figure (4.15), that the tube that will support the fibre is constructed from a ceramic, such as alumina. This is necessary in order to maintain accurate alignment of the fibre optic with the focal point, as the ceramic should not suffer from warp from the possibility of excessive environmental temperatures.

The optical design of the concentrator was finalised using the optical design software package ZEMAX. Figure (4.16) shows the optical design model:

![Figure 4.16: ZEMAX optical design model of cassegrain concentrator unit.](image)

The final optical design of the concentrator was designed for the properties shown in table (4.6).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>RMS surface error</td>
<td>&lt;0.1μm</td>
</tr>
<tr>
<td>F number</td>
<td>0.687</td>
</tr>
<tr>
<td>Concentration ratio</td>
<td>11,100</td>
</tr>
<tr>
<td>Strehl ratio</td>
<td>0.8</td>
</tr>
</tbody>
</table>

### 4.4 Cassegrain Concentrating Unit Testing

Precision Optical Engineering (POE) Ltd manufactured a Cassegrain concentrating unit shown in figure (4.17), for the purposes of this investigation. It consists of two optical components, a primary parabolic concentrator and a flat secondary relay mirror which functions to make the whole assembly more compact and allows the fibre optics to be accurately aligned with the focal point of the primary. The concentrator was designed to couple concentrated light into a 1mm core size fibre optic with a NA of 0.66, thus it has a rim angle of ~40° providing excellent concentration. The concentrator itself weighs ~300 g, this should be compared to weight (15 kg) of a 0.5 m diameter aluminium parabolic concentrator that was constructed for another project at the University of Surrey. It would take 23 of the smaller concentrators to capture the same solar
power as the larger concentrator. Even so, 23 of the smaller concentrators will in total weigh only 6.9 kg, providing an 8.1 kg mass saving.

Figure 4.17: Prototype Cassegrain concentrating unit

4.4.1 Concentrator RMS Surface Form Error

Precision Optical Engineering Ltd, a company based in Hertfordshire, UK, manufactured the primary concentrating parabolic mirror at their diamond turning facility. Testing of the primary concentrating unit at POE provides the following details of the concentrator quality:

- Peak-to-Valley: 0.34 μm (Addition of largest peak and valley)
- RMS surface form error: 0.08 μm.
- Strehl ratio: 0.8 (80% of energy within ideal spot size)
- F number: 0.687 (ratio of focal length to concentrator diameter)

The aluminium primary was probed at POE after completing the diamond turning process to produce the form error graph shown in figure (4.18)

Figure 4.18: Form error graph for prototype Cassegrain concentrating unit
Figure (4.18) plots the variation of the mirror surface from the ideal surface. This plot is a cross section of the mirror surface and is indicative of the whole surface as tooling indentations from the turning process will be made axially symmetrically. The largest peak occurs quite close to the centre of the mirror, which is fortunate as peaks closer to the mirror centre have less impact for the resulting image because they take up less area on the mirror. From figure (4.18) we can estimate a value for the RMS form error of the mirror as 0.08 μm. From this value of the RMS form error we can estimate that the primary has a concentration ratio close to the ideal value of \(-21,100\) and should easily be able to produce a spot size suitable for coupling to a 1 mm core size fibre optic cable. POE quotes the focal length of the primary as 72.1 mm ±0.1 mm.

### 4.4.2 Concentrator Efficiency Testing

On-Sun experiments conducted during the summer of 2005 tested the cassegrain optical units overall efficiency and identified a suitable secondary relay mirror. In these experiments the concentrating unit was employed to heat up a small (1 g), thermally insulated, graphite sample. The initial temperature gradient of the graphite sample upon heating, provides an estimate of the power directed at the graphite sample, figure (4.19). A pyrheliometer is used to determine the intensity of the incident solar radiation and a thermocouple is used to measure the graphite temperature. The experiment was conducted with the concentrator attached to a Losmandy solar tracking tripod mount.

![Image of concentrator setup](image)

**Figure 4.19: Summer test 2005, prototype Cassegrain concentrating unit.**

The primary, rated at 90% efficiency, directed 6.3 W of power at the graphite sample with a local intensity of 800 W/m². The graphite sample obtained a temperature of 450 K over 60 s period.

A number of candidate secondary relay mirrors were selected for use with the Cassegrain concentrating unit. Each secondary had a different surface material and surface quality; these are listed in table (4.7):
Table 4.7: Candidate secondary relay mirrors ($\lambda=450$nm)

<table>
<thead>
<tr>
<th>UNOS Part No</th>
<th>Surface Quality</th>
<th>Surface Material</th>
<th>Efficiency of Secondary</th>
</tr>
</thead>
<tbody>
<tr>
<td>340342</td>
<td>1/2$\lambda$</td>
<td>Aluminium</td>
<td>80%</td>
</tr>
<tr>
<td>340334</td>
<td>1/10$\lambda$</td>
<td>Aluminium</td>
<td>92%</td>
</tr>
<tr>
<td>340621</td>
<td>1/4$\lambda$</td>
<td>Silver</td>
<td>83%</td>
</tr>
<tr>
<td>340618</td>
<td>1/2$\lambda$</td>
<td>Silver</td>
<td>75%</td>
</tr>
</tbody>
</table>

Each secondary relay mirror was tested in a similar manner to the primary, by heating a graphite sample. The graphite sample and secondary were placed along the focal axis of the primary such that the graphite sample always received the same amount of radiant power from the primary. Then by measuring the initial temperature gradient of the graphite sample, an efficiency is calculated for each secondary, as seen in table (4.7). The importance of secondary surface quality is highlighted by these results, with the most efficient secondary having a surface quality of $1/10^6 \lambda$. With this secondary the Cassegrain optical unit will have an overall efficiency of 83%.

4.4.3 Concentrator Spot Analysis

It was of interest to examine the change in concentrator solar image (spot) properties with fibre optic position for the purpose of ensuring an efficient fibre alignment. To experimentally verify the properties of the spot produced by the concentrator it was necessary to employ a controlled solar source to eliminate environmental variations in incident intensity. The Sun sensor facility at SSTL, being equipped with a xenon arc lamp solar simulator, was deemed suitable for such testing. The experimental setup for the measurement is shown in figure (4.20):

![Figure 4.20: Experimental set-up for concentrator intensity distribution measurement.](image-url)
lens are aligned via an optical rail which lies parallel to the beam propagation direction. A two-directional micrometer stage is also on the optical rail located at the concentrator focal point between the concentrator and collimating lens. This micrometer stage supports a 2m long FLU fibre optic cable that runs between the concentrator focal point and an ORIEL thermopile radiative power sensor. A fibre is used because it is able to sample a much smaller area of the spot than the thermopile sensor and blocks less incident light than the thermopile sensor. The micrometer stage allows small accurate movements of the fibre tip with respect to the concentrator focal point, which aids in accurately locating the focal point and allows small deviations from the focal point to be made for the purposes of the experiment. Upon finding the focal point with the fibre optic tip, fine movements of the fibre tip are made perpendicular to the concentrator optical axis. This results in a variation of the radiative power measured by the thermopile sensor that is indicative of the size and shape of the intensity distribution on the fibre tip. The diameter of the fibre is 1mm and, therefore, only intensity values averaged over the area of the fibre tip can be obtained. Figure (4.21) plots the fibre-transmitted power vs. focal plane radial position for a number of focal axis (z) positions (focus + 1mm and focus + 2mm).

![Graph showing fibre-transmitted power vs. spot radial position for varying focal axis position](image)

**Figure 4.21:** Fibre probe transmitted power vs. spot radial position for varying focal axis position (error ±0.01 W).

In figure (4.21) it can be seen that the transmitted power is very sensitive to small changes in fibre position, as expected. However, it is worth noting that changes in position in the z direction result in less dramatic drops in power transmission in comparison to changes in the radial direction. The shapes of the curves in figure (4.21) are misleading when consideration is given to the intensity distribution of the concentrator spot. The attenuation due to transmission through the fibre reduces the measurement of the power incident on the fibre tip. If the fibre transmission loss can be said to
be 30% (in accordance with Feuermann and Gordon [9, 1999] and Nakamura et al [59, 2002]), the power incident on the fibre tip at the focal point of the concentrator is ~ 6.5 W and an average intensity of $8.2 \times 10^6 \text{Wm}^{-2}$ over the fibre tip results. The power reflected by the concentrator on to the spot is 9.8 W. As the transmission loss (verified by Feuermann and Gordon [9, 1999] and Nakamura et al [59, 2002]) only accounts for a loss of 30% while a loss of 54% is observed, indicates there is another source of loss. Figure (4.21) shows the fibre transmitting power at the 1 mm position, beyond the edge of where the spot should end. This implies that the spot is larger than the ideal spot size as the remaining power from the concentrator is not intercepted by the fibre. The effective size of the actual spot can be estimated based on this average intensity value to be ~1.5 mm (± 0.25 mm) in diameter. The reason for this increase in concentrator spot size is most probably due to imperfect collimation from the solar source introducing angular errors. The curves in figure (4.21) also give the impression that the concentrator spot is larger than it should be; this is found to be partly due to the size of the fibre tip as even though the centre of the fibre is located at a particular position, part of the fibre extends closer towards the centre of the spot supporting the assertion that the spot is actually larger rather than the fibre having a larger attenuation. Furthermore, assuming that the spot is larger than the fibre tip, figure (4.21) appears to be consistent with the gaussian like behaviour of the intensity distribution as indicated by Nicolás [44, 1987]. This is demonstrated via the gaussian curve also plotted in figure (4.21), which is the integration of the assumed gaussian concentrator spot intensity distribution (sigma = 0.25, effective diameter 1.5 mm) over the fibre tip area, which was done in order to simulate the expected fibre response. The evaluation of this integration is discussed later in chapter 7 section 7.2.1.

During this experiment, it was noticed that as power was transmitted along the FLU fibre optic, the fibre exhibited a soft blue glow, which is attributed to a combination of photoluminescence, Rayleigh scattering and the imperfect total internal reflection discussed by Feuermann et al [55, 2002] (see chapter 5). The intensity of this glowing varied with transmission power and was visible to the human eye even at powers as small as 0.1 W.

### 4.5 Chapter Summary and Conclusions

#### 4.5.1 Chapter Summary

In this chapter the investigation of developing a suitable concentrator system for a fibre augmented solar thermal propulsion demonstration system has been described. Initially, fixed, rigid, parabolic concentrators were identified to be most appropriate for a small-satellite STP system and the theory of concentrator optic and fibre optic coupling were reviewed. The benefits
of fibre augmentation were demonstrated via the decrease in concentrator system mass by using multiple small concentrators and appropriate space mirror materials was identified. Detailed modelling of a concentrator provided the design parameters necessary for the concentrator manufacture. Finally, a series of practical tests were performed to assess the manufactured concentrators efficiency.

4.5.2 Chapter Conclusions

From the analysis and discussion in this chapter the following conclusions were made:

- Parabolic concentrators are the most suitable optical concentration devices for a small-spacecraft STP system due to highest potential concentration ratio (12,000).

- Parabolic concentrator design parameters are highly dependant on the chosen fibre optic cable for optimal concentrator to fibre coupling. For example, to efficiently couple radiative solar energy from a concentrator into a single fibre optic with a 1 mm diameter core size requires that the concentrator must have an aperture diameter no larger than ~100 mm.

- Ashby and Jones [74, 1980] and Holota et al [75, 1998] space mirror mass as a function of diameter relations, demonstrate significant mass savings in employing multiple small concentrators for a given collection power requirement. For example, a 33% mass saving is observed between employing a single mirror and a concentrator system consisting of 3 mirrors but only a further 9% mass saving (42% overall is observed for a concentrator system consisting of 6 mirrors.

- Optical switching concept demonstrates potential efficiencies comparable to existing optical switch technologies. Discussed further in chapter 7 section 7.1.4.

- Optical surface quality of <0.1 μm is required to achieve maximum concentrations.

- Cassegrain concentrator unit demonstrated an 83% efficiency, demonstrating potential for a high efficiency energy delivery system.

- Cassegrain concentrator spot exhibits Guassian like intensity distribution, consistent with that reported in the literature for non-perfect parabolic concentrator surfaces.

- In laboratory conditions achievable concentration ratio was limited to 5,000 (spot diameter estimated at 1.5 mm) due to angular errors in the solar source. This limited achievable transmission power.
Chapter 5

5 Fibre Optics for STP

This chapter describes the work undertaken to investigate fibre optics suitable for an STP system and how concentrated solar energy can be efficiently coupled into them. To facilitate this discussion, the mission requirements in chapter 3 are used to develop concentrator system components. Through consideration of individual components, design trades and analyses are identified and essential theory is discussed.

5.1 Fibre Optics

In this section the optical and physical properties of fibre optics are discussed in terms of requirements for an STP system. The practical concerns of employing fibre optics for the transmission of high intensity solar energy in a space environment are addressed and candidate fibre optics are identified.

5.1.1 Fibre Optic Properties

A fibre optic suitable for a STP application should have the following properties:

1. Large number of propagation paths
2. Large numerical aperture (NA)
3. Low attenuation over the solar spectrum (from ~300 nm to ~2000 nm)
4. Extreme temperature resilience
5. Radiation and solarization resilience

A large number of propagation paths can be provided by a large diameter multimode step index or graded index fibre optic. A large numerical aperture will allow the fibre optic to accept and transmit a higher intensity of light from the concentrator. Light from a parabolic concentrator mirror will be incident at angles up to the rim angle of the concentrator (\( \Psi_c \)), so it is important for the fibre optic to be able to accept and transmit this incident light. The acceptance angle (\( \Psi_c \)) of a fibre optic is defined by its numerical aperture (NA) which is related directly to the refractive indices of the core and cladding (\( n_{core} \) and \( n_{clad} \) respectively, equation (5.1)) [83, 1987].
The successful transmission of light from a parabolic mirror through a fibre optic requires that \( \psi_0 \geq \psi_c \). For a fibre optic to operate efficiently in a STP system it should have a low attenuation over the solar spectrum. Solar radiance is greatest, at >0.2 Wm\(^{-2}\) in a spectral range of 300nm to 2200 nm, corresponding to the UV and NIR regions of the EM spectrum. A fibre optic is required that is near transparent in this wave band. Pure fused silica fiber optic cables are commonplace within the optical communications industry due to their very low attenuation over the optical waveband. A typical use of fused silica optical fiber is for single mode long distance data/communications cables. For STP, as observed regularly in the literature, fused silica fibre optics offer an excellent compromise between available NA, spectral coverage, attenuation and intrinsic resilience to radiation and temperature. Therefore these fibers are considered in detail for this application.

### 5.1.2 Fibre Optic Attenuation

<table>
<thead>
<tr>
<th>Material</th>
<th>Category</th>
<th>Wave Band</th>
</tr>
</thead>
<tbody>
<tr>
<td>Silica (Low OH)</td>
<td>Glass</td>
<td>UV – Vis (180nm – 3000nm)</td>
</tr>
<tr>
<td>Sapphire</td>
<td>Crystal</td>
<td>Vis – MidIR (500nm – 3100nm)</td>
</tr>
<tr>
<td>Zirconium Fluoride</td>
<td>Glass</td>
<td>Vis – MidIR (450nm – 5000nm)</td>
</tr>
</tbody>
</table>

For comparison, a number of candidate fibre materials are shown in table (5.1). These materials are free of molecular rotational, molecular vibrational and electron transition energy states within the visible region of the spectrum. Molecular rotational and vibrational states tend to occur further into the IR. The electron transition energy states for these materials occur approaching UV photon energies. These facts mean these materials have a good transmission window between the UV and IR absorption bands. A very problematic contaminant of fused silica fibre optics in the visible waveband is the hydroxyl ion (OH\(^-\)).

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\( \psi_0 \) = Ultra Violet, Vis = Visual, NIR = Near Infrared, MidIR = Medium Infrared
OH gives rise to a fundamental vibrational absorption at 2.73 μm and 4.2 μm, and more importantly, harmonics at 1.38, 1.24, 0.95 and 0.72 μm. The presence of OH is usually attributed to an abundance of water vapour during the fibre manufacture. Transition metal impurities also give rise to unwanted absorption mechanisms. The concentration of these contaminants can be minimized via fine control of the manufacturing environment, although these processes tend to be expensive. The attenuation spectrum of a low OH pure fused silica fibre optic is shown in figure (5.2) superimposed upon the solar spectrum [86, 2005]. Figure (5.2) indicates that most absorption occurs within the UV portion of the solar spectrum.

Another form of intrinsic attenuation is via Rayleigh scattering. This is caused by microirregularities within the core glass. These irregularities tend to arise from density and composition fluctuations within the glass itself. The scale of these irregularities are small compared to the scattered wavelengths, the degree of Rayleigh scattering is wavelength dependant and is proportional to the negative 4th power of the wavelength in bulk media ($\nu^{-4}$)[85, 2002][87, 1979]. The occurrence of these irregularities can be minimised via careful manufacture of a fibre optic such that the core material is able to reach a state of thermal equilibrium. Attenuation spectrums quoted by manufacturers are for both core absorption and scattering [87, 1979]. For fused silica fibre optics, Rayleigh scattering losses have been reported to be as high as 0.15 dB/km at 1.550 nm [88, 2003].

Another source of intrinsic light loss is photoluminescence, which is a result of excitation of electron energy states in core defects and impurities. Core defects refer to the presence of core material molecules having formed structural bonds with other elements; Sakurai and Nagasawa [89, 2001] investigated the origins of defect related photoluminescence in silica glass and observed correlations between observed photoluminescence bands and oxygen related vacancies.
Core impurities refer to the presence of foreign molecules within the core material such as hydroxyl ions or chlorine groups. Photoluminescence of silica glass is a subject studied extensively in the literature; as indicated via several references presented in the bibliography [89, 2001][90, 2003][91, 2003]. The reason for this study is the potential for silica and silicon material photoluminescence opto-electronic devices.

Figure 5.3: (a) Blue color photoluminescence observed in oxygen atmosphere. (b) Intense white color photoluminescence observed in vacuum. [91]

Mochizuki and Araki [91, 2003] demonstrate the change in the photoluminescence (observed as a soft glowing of the glass material) of silica glass, illuminated by a 325nm laser, when in vacuum and oxygen environments, as seen in figure (5.3). Attenuation of light transmitted along a fiber optic can also be increased by external influences. These are outlined in table (5.2).

<table>
<thead>
<tr>
<th>External Influence</th>
<th>Attenuation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Macro bend</td>
<td>Physical bend in the fibre. Has the effect of reducing the number of higher order propagation modes available. Figure (5.4b)</td>
</tr>
<tr>
<td>Micro bend</td>
<td>Fibre subjected to physical stresses, leading to unrecoverable distortions along the core material. Figure (5.4b)</td>
</tr>
<tr>
<td>Core/Cladding scratches</td>
<td>Damage to either core or cladding can result in fibre breakage and a breakdown in the local total internal reflection. A suitable buffer and jacket typically protect a fibre optic.</td>
</tr>
<tr>
<td>Elevated temperatures</td>
<td>Exposure to temperature exceeding the operational range of the fibre can lead to a breakdown in the fibres optical properties.</td>
</tr>
<tr>
<td>Radiation exposure</td>
<td>Exposure to radiation results in the formation of defects (color centers) in the fibre. These, in turn, absorb light.</td>
</tr>
<tr>
<td>Solarization</td>
<td>This effect is caused by exposure to very intense UV light, thus forming centers of excited electron energy states.</td>
</tr>
</tbody>
</table>

To ensure the minimum amount of non-intrinsic attenuation, the fibre should be protected from these influences.
Macro bends in the fibre have the effect of altering the incidence angle of a light ray approaching the core/cladding interface. If this ray incidence angle exceeds the critical angle\(^2\) of the fibre optic, then the ray will propagate into the cladding. Fibre optics with large numerical apertures can withstand larger macro bends before suffering losses. Ideally, a fibre operating in a STP system should be as flexible as possible without adding to its intrinsic attenuation. The fracture bend radius of a fibre optic is often proportional to a fibre’s core diameter and quoted by the manufacturer. For silica fibre, this is typically 300 times the core radius. Smaller bends are achievable with smaller core sizes, however, the desire for a flexible fibre conflicts with that for a large fibre optic core diameter to reduce concentrator tolerances. A compromise can be met by using a fibre optic bundle of suitably flexible fibres, constructed for a given concentrator. This, however, will result in packing losses due to gaps between neighbouring fibres. Liang et al [50, 1998] was successful in constructing fibre optic bundles of 7 and 19 fibres that were tightly packed via polishing the fibre tips into closely-fitting hexagonal shapes, see figure (2.16a). This minimized bundle-packing losses and on transmission of concentrated solar light, 60% overall efficiency was observed. To avoid micro bends, the fibre optic would be sheathed in Kevlar fibres within the fibre jacket for support, figure (5.4a). The fibre optic cable cannot be allowed to have very sharp bends along its length. A suitable buffer would protect the fibre from surface scratches and wear. As the fibre is likely to experience heating and transverse temperature gradients a buffer able to withstand high temperatures would be appropriate. For silica fibres, silicone or acrylate are appropriate options with an operating temperatures of up to 400°C and 300°C respectively, with acrylate being the most common [86, 2005].

A fibre optic transmitting concentrated solar energy will experience heating through absorption of light energy and also through any conductive contact the fibre optic has with the solar thermal processes.

\(^2\) The critical angle of a fibre optic is the maximum light ray incidence angle at the core cladding interface at which total internal reflection will occur. [85, 2002][87, 1979]

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receiver. In 1999, Jaramillo et al [93, 1999] demonstrated that a silica fibre optic operating terrestrially could function for up to six hours and remain within its working temperature limits. Jaramillo et al demonstrated this numerically, employing an attenuation spectrum with higher attenuation than that shown in figure (5.2). A one-dimensional, transient, finite difference thermal simulation analysis of a fibre operating in a vacuum was performed to ascertain the likely thermal environment of the silica (low OH) fibre selected for use with the STP system. The attenuation spectrum of figure (5.2) was used for this analysis, and radiative power loss was included in the finite difference calculations. The simulation geometry is shown in figure (5.5):

![Figure 5.5: One-dimensional finite difference thermal simulation geometry of a fibre optic transmitting concentrated solar energy from a solar concentrator.](image)

The one-dimensional, transient energy equation for coupled conduction and radiation in a participating medium is given as:

$$\rho \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial z^2} - \nabla \cdot \bar{q} + q^* \quad \text{(5.2)}$$

where $\rho$ is core material density (value given in figure (5.5)), $C_p$ is core specific heat capacity (value given in figure (5.5)), $T$ is the fibre optic cable temperature (a function of spatial coordinate $z$ and time $t$), $k$ is core material thermal conductivity (value given in figure (5.5)), $\bar{q}$ is the radiative heat flux vector and $q^*$ is internal heating of the fibre. The radiative heat flux is given by:

$$\nabla \cdot \bar{q} = \int \nabla \cdot \bar{q}_\nu \, d\nu = \int 4\pi \frac{\kappa_\nu}{\omega_\nu} \left[ \frac{\sigma T^4}{\pi} - S_\nu \right] \, d\nu \quad \text{(5.3)}$$

where $\kappa_\nu$ is spectrally dependent absorbivity of the fibre (obtained from the fibre attenuation spectrum), $\omega_\nu$ is spectrally dependent scattering albedo and $\kappa_\nu$ is spectrally dependent "source function" representing the instantaneous intensity distribution due to radiating medium. For the purposes of the one-dimensional simulation equation (5.3) is simplified via assuming blackbody
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Emission and that the effect of the source function is negligible which is valid for one-dimensional geometry, to obtain:

$$\nabla \cdot \vec{q} = 4\sigma T^4 \quad \ldots (5.4)$$

Heating of the fibre is accomplished via the internal heating term:

$$q'' = \int^{2000\text{nm}}_{300\text{nm}} q''(v) dv = \int^{2000\text{nm}}_{300\text{nm}} C I_v e^{-\kappa_v z} dv \quad \ldots (5.5)$$

where C is the concentrator concentration ratio and \(I_v\) is the solar intensity spectrum. The internal heating term is a function of \(z\) and therefore has a different value at each point in the fibre; however, it is not time dependent and therefore need only be evaluated once. The internal heating term is spectrally dependent and is integrated over the solar spectrum (from 300 nm to 2,000 nm).

Equation (5.2) is solved via an explicit finite difference technique in which the one-dimensional, transient, geometry of the fibre is represented via a series of discrete points, \(N\) spatial coordinate (\(z\)) points and \(M\) transient (\(t\)) points, as shown in figure (5.6).

Figure 5.6: One-dimensional, transient, finite difference discreteisation.

From figure (5.6) the explicit finite difference mathematical formulation of equation (5.2) is:

$$\rho c_p \frac{T(z_n, t_m) - T(z_n, t_{m-1})}{\Delta t} = k \frac{T(z_{n+1}, t_{m-1}) - 2T(z_n, t_{m-1}) + T(z_{n-1}, t_{m-1})}{\Delta z^2} - \nabla \cdot \vec{q} + q'' \quad \ldots (5.6)$$

With an initial temperature distribution at \(t=0\), equation (5.6) is evaluated at each discrete \(z\) point (equally spaced \(\Delta z\) apart) in the fibre geometry to find the temperature distribution of the fibre at a given time \(t_n\). Once the temperature distribution of the fibre has been calculated for a given time \(t_n\), the process is repeated to find the temperature at a time \(\Delta t\) later. The radiative heat flux term is evaluated every time step and updated for the next iteration, necessitating small time steps in order to achieve an accurate and stable simulation.

Figure (5.7) shows the heating profile resulting from the thermal simulation of the fibre tip at the end of the fibre coupled to the concentrator system.
Pure fused silica can operate at temperatures as high as 1,000°C [59, 2002]. What limits the operational temperature range is the cladding, buffer and jacket employed. Table (5.3) presents a number of fused silica core fibre optics that are currently available commercially.

### Table 5.3. Fused silica core, commercially available fibre optic cables

<table>
<thead>
<tr>
<th>Product</th>
<th>Manufacturer</th>
<th>Cladding</th>
<th>Buffer</th>
<th>NA</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>FLU</td>
<td>Polymicro</td>
<td>Teflon</td>
<td>Silicone</td>
<td>0.66</td>
<td>-10 to 160</td>
</tr>
<tr>
<td>STU-D</td>
<td>Polymicro</td>
<td>Doped Silica</td>
<td>Acrylate</td>
<td>0.22</td>
<td>-65 to 300</td>
</tr>
<tr>
<td>Optran Ultra</td>
<td>CeramOptec</td>
<td>Silica</td>
<td>Polymide</td>
<td>0.37</td>
<td>-190 to 400</td>
</tr>
<tr>
<td>Optran UVNS</td>
<td>CeramOptec</td>
<td>Fluorine doped silica</td>
<td>Silicone</td>
<td>0.30</td>
<td>-190 to 400</td>
</tr>
<tr>
<td>HCN-N1000T-14</td>
<td>SpecTran</td>
<td>Hard Polymer</td>
<td>Tefzel</td>
<td>0.44</td>
<td>-100 to 125</td>
</tr>
<tr>
<td>SFS-T</td>
<td>Fiberguide</td>
<td>Doped Silica</td>
<td>Polymide</td>
<td>0.26</td>
<td>-190 to 350</td>
</tr>
</tbody>
</table>

Table (5.3) suggests that there is a trade-off between fibre temperature range and NA. The reason for this trade-off is likely because for the low NA (NA < 0.37) fibres in table (5.3), the cladding materials are silica based, which will subsequently have thermal properties similar to that of the fused silica core material. However, because the cladding materials are silica based their refractive indices will be closer to the core material in comparison to the other cladding materials in table (5.3). This trade-off will impact the thermal design, potential efficiency and robustness.

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22 Fibre data obtained from manufacturers websites.
of the STP system. Silica’s fictive temperaturé $^{23}$, similar to its melting temperature, is approximately the same as the minimum desired operating temperature of the solar receiver, 1600°C. This implies that in coupling the fibre optic to a solar receiver, the fibre should not come into direct thermal contact with the receiver. An STP subsystem analysis tool, written by Kennedy [1, 2004], is used to determine the variation in outer insulation temperature for varying AETB-12 insulation thickness. Figure (5.8a) shows the trend in outer insulation temperature with increasing insulation thickness for a constant receiver temperature of 1600°C.

![Figure 5.8: (a) Outer insulation temperature for increasing insulation thickness, receiver temperature 1600K (b) Fiber-receiver coupling concept.](image)

Figure (5.8a) implies that a fibre optic can be mechanically supported by suitably thick insulation and remain within its working temperature range during heating of the receiver. The lower the maximum operating temperature of the fibre the more insulation will be required to protect it, leading to a bulkier receiver assembly. The fibre would protrude into the receiver without physical contact with the receiver as depicted in figure (5.8b). In this case the fibre would be in direct radiative contact with the receiver and indirect conductive contact. The fibre would protrude from a ceramic tube, which maintains the fibres rigidity and acts as further insulation along the fibres length.

In recent years, a major effort has been devoted to the study of ionising radiation upon silicon dioxide glass. The purpose of this study was to develop radiation-resistant glass for fusion plasma diagnostics. Brichard et al [94, 2002][95, 2001] observed the growth of the first overtone of the OH absorption peaks in polymer coated silica fibre and also reports that in aluminium jacketed silica fibre this growth did not occur. Brichard et al [96, 2004] also discusses a method for developing radiation hardened fibres via a hydrogen treatment of the glass core. A fibre optic

$^{23}$ The fictive temperature is the temperature at which the glass can reach a state of thermal equilibrium due to the frozen in structure of the glass breaking down and the glass material becoming molten.[76]
operating in space will be subjected to radiation exposure, and hence, its optical performance will degrade over time. A typical dose rate of radiation in low earth orbit is 5-10 krads/year, increasing for higher inclinations and increased solar activity [2, 1999]. Brichard et al observes a 12 dB/m increase in attenuation at 1,380 nm in silica for a dose rate of 590 Mrads/hr and a total dose of 3.3 Grads. A survey conducted by the NASA Goddard Space Flight Centre [97, 2002] compiled data on gamma ray induced attenuation for a number of commercial fibre optics. From this survey, it was discovered that low-OH fused silica fibres would suffer induced attenuations at 750 and 850 nm on the order of 0.6 dB/m for doses on order of 250 krads. This suggests that in a low earth orbit, and with proper fibre optic cabling/shielding a fibre optic cable could be used for the lifetime of a small-satellite (~5 years) without exhibiting an incapacitating increase in attenuation. For higher altitudes the Van Allen radiation belts and solar activity increases the likely radiation doses experienced, with dose rates reported as high as 50 rads/hr [98, 1998]. For the HLEO mission discussed in chapter 3 the manoeuvre duration is 402 hours, which would result in a manoeuvre radiation dose of 20 krads. This suggests that silica fibre optics could survive and be used for this application with appropriate cabling /shielding. Missions further a field of LEO would undoubtedly require more fibre optic radiation protection. Table (5.4) summarises the radiation induced absorptions and expected increase in fibre attenuation for the DMC inspector and HLEO constellation small-spacecraft missions:

<table>
<thead>
<tr>
<th>Small-spacecraft mission</th>
<th>DMC Inspector</th>
<th>HLEO Constellation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mission life time</td>
<td>5 years</td>
<td>402 hours</td>
</tr>
<tr>
<td>Dose rate</td>
<td>10 krads/year (LEO)</td>
<td>50 rads/hr (GEO)</td>
</tr>
<tr>
<td>Total dose</td>
<td>50 krads</td>
<td>20 krads</td>
</tr>
<tr>
<td>Estimated increase in fused silica attenuation at 750 nm over mission lifetime.</td>
<td>0.12 dB</td>
<td>0.05 dB</td>
</tr>
<tr>
<td>Estimated increase in fused silica attenuation at 850 nm over mission lifetime.</td>
<td>0.12 dB</td>
<td>0.05 dB</td>
</tr>
<tr>
<td>Estimated increase in fused silica attenuation at 1,380 nm over mission lifetime.</td>
<td>$1 \times 10^{-4}$ dB</td>
<td>$0.5 \times 10^{-4}$ dB</td>
</tr>
</tbody>
</table>
From the table (5.4) it can be seen that the induced attenuations in fused silica fibre optics, (assuming the scale of these induced attenuations are representative of the attenuation spectrum over the wave band of interest) from radiation exposure represents approximately a 1% decrease in system efficiency over the system lifetime. Such a decrease in system efficiency would easily be tolerable in comparison to the inefficiencies experienced with other system components (for example the solar concentrator reflective efficiency is around 95%).

Solarization\textsuperscript{24} in fibre optics causes the formation of colour centres in the core silica glass. Impurities within the core form unbound electron pairs on the Si atom, which are affected by UV radiation to form colour centres causing massive absorptions between 215-254nm and further into the UV region. This effect is outside the waveband of interest, and attenuation recovers at approximately 300nm. Solarization resistant fibre optics exist commercially, but due to their hydrogen loaded construction, they have a limited life time due to hydrogen off-gassing from the core. Nevertheless, solarization resistant fibres are continuing to be developed commercially and are preferable to minimize the formation of colour centres [99, 2004][100, 2005]. Furthermore, in some cases solarization resistant fibres have been shown to demonstrate good resistance to radiation exposure [101, 2005].

5.1.3 Fibre Optic Selection for a STP System

A range of fused silica fibre optics was considered for the purposes of experimental system testing. The choice of fibre optic impacts greatly on all other system components, as well as the overall efficiency of the STP system. There is a further type of attenuation, not mentioned in the previous section, which has been observed experimentally in fused silica core fibre optics transmitting high intensity solar radiation. Feuermann et al [55, 2002] refer to this attenuation as “light leakage” and attributes the phenomenon to imperfect total internal reflection. Feuermann et al suggest this results from the imaginary component of the claddings refractive index which, although causes a very small loss on one reflection, accumulates a larger loss for more reflections. Therefore indicating this loss increases for larger incident light ray angles (and therefore fibre optic NA) and fibre optic length. Table (5.5) lists the commercial fibre optics considered for the experimental development of an STP demonstrator system.

\textsuperscript{24} Solarization is a term describing the absorption of energetic ultra violet radiation in silica at wavelengths less than 200 nm.
Fibre Optic Applications in Solar Thermal Propulsion Systems

Table 5.5: Considered commercial fibre optics

<table>
<thead>
<tr>
<th>Product</th>
<th>Manufacturer</th>
<th>Cladding</th>
<th>Standard Buffer</th>
<th>NA</th>
<th>Temperature Range (°C)</th>
<th>Available Core (mm)</th>
<th>Max Diameter</th>
</tr>
</thead>
<tbody>
<tr>
<td>FLU</td>
<td>Polymicro</td>
<td>Teflon</td>
<td>Silicone</td>
<td>0.66</td>
<td>-10 to 160</td>
<td>1000</td>
<td></td>
</tr>
<tr>
<td>STU-D</td>
<td>Polymicro</td>
<td>Doped Silica</td>
<td>Acrylate</td>
<td>0.22</td>
<td>-65 to 300</td>
<td>1000</td>
<td></td>
</tr>
<tr>
<td>FLHAC</td>
<td>Polymicro</td>
<td>Hard Polymer</td>
<td>Tefzel</td>
<td>0.33</td>
<td>-100 to 125</td>
<td>1000</td>
<td></td>
</tr>
<tr>
<td>TW600/620 TAF</td>
<td>CeramOptec</td>
<td>Teflon</td>
<td>N/A</td>
<td>0.66</td>
<td>-10 to 125</td>
<td>600</td>
<td></td>
</tr>
<tr>
<td>Optran Ultra</td>
<td>CeramOptec</td>
<td>Silica</td>
<td>Polymide</td>
<td>0.37</td>
<td>-190 to 400</td>
<td>600</td>
<td></td>
</tr>
<tr>
<td>Optran UVNS</td>
<td>CeramOptec</td>
<td>Silica</td>
<td>Polymide</td>
<td>0.30</td>
<td>-190 to 400</td>
<td>1700</td>
<td></td>
</tr>
<tr>
<td>HCN-N1000T-14</td>
<td>SpecTran</td>
<td>Hard Polymer</td>
<td>Tefzel</td>
<td>0.44</td>
<td>-100 to 125</td>
<td>1400</td>
<td></td>
</tr>
<tr>
<td>SFS-T</td>
<td>Fiberguide</td>
<td>Doped Silica</td>
<td>Polymide</td>
<td>0.26</td>
<td>-190 to 350</td>
<td>770</td>
<td></td>
</tr>
<tr>
<td>FT1EMT</td>
<td>3M</td>
<td>TECSTM</td>
<td>Tefzel</td>
<td>0.39</td>
<td>-100 to 125</td>
<td>1000</td>
<td></td>
</tr>
</tbody>
</table>

From table (5.5) the fibre optic selected for further experimental development was Polymicro’s FLU fibre optic. This fibre was chosen for its large numerical aperture, large core diameter and moderate temperature range. The main concern with choosing the FLU fibre is the amount of light-leakage attributed to such a high NA. This light-leakage loss could be reduced by removing the cladding material from the silica core, which would have the effect of increasing the critical angle at the boundary of the core material, providing better support for high order propagation paths. However, on exposing the core there is a risk that the core material could be damaged and significant transmission losses could occur. This loss of power must be compared to the drop in system efficiency attributed to the larger receiver aperture sizes required for lower NA fibre optics. Table (5.6) compares three fibre optics with different NA values in terms of light leakage and achievable concentration.

Table 5.6: Comparison of commercial fibre optics with varying numerical apertures.

<table>
<thead>
<tr>
<th>Fibre</th>
<th>FLU</th>
<th>HCN-N1000T-14</th>
<th>FLHAC</th>
</tr>
</thead>
<tbody>
<tr>
<td>NA</td>
<td>0.66</td>
<td>0.44</td>
<td>0.33</td>
</tr>
<tr>
<td>Normalised Light Leakage (%)</td>
<td>18 (3m length)</td>
<td>9 (5m length)</td>
<td>4 (3m length)</td>
</tr>
<tr>
<td>Appropriate Concentrator Rim Angle</td>
<td>41.3°</td>
<td>26.1°</td>
<td>19.2°</td>
</tr>
<tr>
<td>Concentrator Spot Size for a 10cm Diameter Parabolic Concentrator (mm)</td>
<td>1</td>
<td>1.3</td>
<td>1.5</td>
</tr>
<tr>
<td>Concentrator Focal Length for a 10cm Diameter Parabolic Concentrator (mm)</td>
<td>72</td>
<td>114</td>
<td>155</td>
</tr>
<tr>
<td>Theoretical Concentration Ratio</td>
<td>11200</td>
<td>7100</td>
<td>4300</td>
</tr>
<tr>
<td>Theoretical Peak Receiver Temperature (K) (obtained from equation (6.8) with including efficiency, see chapter 6, section 6.1.3)</td>
<td>3050</td>
<td>2896</td>
<td>2629</td>
</tr>
</tbody>
</table>
Comparing the fibres in table (5.6) the FLU can be seen to be associated with a parabolic concentrator with a significantly higher concentration ratio in comparison to the other fibres in the table. A larger achievable concentration ratio allows a smaller receiver aperture, resulting in a high receiver peak temperature; this is discussed further in chapter 6. A further advantage is that the design of the concentrator assembly can be smaller due to the smaller focal length, also shown in table (5.6).

The light leakage values given in table (5.6) refer to the decrease in transmission integrated over the numerical aperture of the fibre. Therefore, the real issue is how large to make the rim angle of a suitable concentrator as the FLU fibre will demonstrate a smaller light-leakage if it was integrated up to a smaller NA value. Choosing the FLU fibre therefore provides the option of having high rim angle concentrators, but would work just as well for small rim angle concentrators. This is demonstrated in figure (4.3a) [55, 2002].

5.1.4 Fibre Optic Efficiency

The fibre optic selected for testing was Polymicro's FLU fibre. The work conducted by Feuermann et al [55, 2002] investigated light leakage in the FLU fibre optics, indicating that the maximum transmission efficiency integrated over the numerical aperture of the fibre was 82% for a three metre length of fibre. These lengths of fibre are on the scale to be used for the STP demonstrator system. Therefore this value is taken as the maximum efficiency of the fibre. This loss will include Fresnel reflection losses at both ends of the fibre.

5.2 Fibre Optic Thermal Modelling

Of critical concern for a fibre augmented STP system is the thermal environment to which the fibre optics are exposed. It is clear that given the high temperatures required of the cavity receiver (>1,000K) the fibre couldn’t be allowed to come into direct contact with the receiver. However the fibres will be exposed to high intensity radiation, at the concentrator tip of the fibre and to a lesser extent at the receiver end of the fibre. Therefore, it was considered important to ascertain the thermal behaviour of the fibre exposed to such conditions in a vacuum environment. Several references report similar studies: for example Jaramillo et al [93, 1999] employs a two dimensional finite difference algorithm to study the thermal behaviour of silica fibre optics channelling high intensity solar radiation in a terrestrial (convective) environment. Nakamura et al

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25 In optics Fresnel reflection is the reflection of a portion of incident light at a discrete interface between two media having different refractive indices. Fresnel reflection occurs at the air-glass interfaces at the entrance and exit ends of an optical fibre. Gordon et al [51, 2003][52, 1999] state a Fresnel reflection loss of 4% at each air-fibre interface (entrance and exit) at normal incidence of a fibre optic transmitting concentrated solar energy.
[59, 2002] report on an experimental study in which a bundle of silica fibre optics are exposed to intense solar radiation in a vacuum chamber and obtain temperatures approaching 500°C; although in this case the concentration ratio of the concentrator mirrors was much lower than in the present case. In the process of investigating the thermal behaviour of silica fibre a novel heat transfer algorithm was developed for the study of coupled conduction and radiation heat transfer within participating media. The algorithm applies analytic solutions to the conductive and radiative transient energy heat transfer equations of the media coupled with a numerical leapfrog algorithm, allowing for more stable and accurate estimates of the medium's thermal behaviour for less computational demand in comparison to standard solution methods. In the following subsections this unique algorithm is described, validated and then employed to assess the thermal behaviour of the silica fibre optic selected for use in the UK-DMC demonstrator STP system.

5.2.1 A Leapfrog Algorithm for Coupled Conductive and Radiative Transient Heat Transfer in Participating Media

Coupled conductive and radiative heat transfer within participating media is an important area of study for many industrial applications. Porous, fibrous and semi-transparent materials are examples of media that can exhibit thermal behaviour in which radiative heat transfer plays an important role. Consequently numerous cases of this type of problem for varying mediums and geometries have been considered in the literature. In modelling such problems, a non-linear integrodifferential energy equation is formulated, which is typically a complicated matter to solve. Some of the earliest references discussing these problems date back to the 1960s. For example, Viskanta and Grosh [60, 1962] apply a numerical integration and iteration technique to obtain the temperature distribution of a plane layer of grey gas between infinite, black, parallel plates. Since then, a variety of other techniques have been employed to solve a myriad of problems that consider different material properties and different geometries. For example, Tan et al [61, 2004] employ a nodal analysis based on Hottel’s zonal method to treat transient coupled radiative and conductive heat transfer within non-grey, semi-transparent materials. Tan et al [61, 2004] also employ a ray-tracing method and Hottel and Sarofim’s zonal method to assess one-dimensional, transient, coupled radiative and conductive heat transfer in a multi-layer absorbing and isotropic scattering composite. Wu et al [62, 1997] use a discrete ordinate method to investigate heat transfer in a two-dimensional, cylindrical, scattering medium with Fresnel boundaries.

This work presents the first applications of a technique that employs an iterative leapfrog algorithm to solve coupled conductive and radiative heat transfer problems within participating media. This algorithm was developed for the study of heat transfer within fibre optic cables transmitting intense solar radiation in order to assess the thermal behaviour of these fibres. In section 5.2.2.1 this algorithm is employed to simulate two-dimensional fibre heating when
transmitting concentrated solar energy from a parabolic dish concentrator, in the same manner as the one-dimensional explicit finite difference calculation in section 5.1.2. The motivation behind the algorithm development was to minimise computation time while retaining acceptable accuracies, over several hours of simulation time. It was found when using the one-dimensional explicit technique that only very small simulation time steps provided sufficient algorithm stability. Thus, an attempt was made to incorporate analytical heat transfer expressions into a numerical algorithm to combat stability and accuracy issues resulting from large simulation time-steps. An approach making use of a leapfrog algorithm was found to be suitable.

The Verlet algorithm was devised by L. Verlet in the early days of dynamical molecular simulation [63, 1995]. The leapfrog algorithm is a modified version of the Verlet algorithm. These types of algorithm are well known and are usually applied to control and dynamical problems. A typical control application of a leapfrog algorithm is to determine optimal linear quadratic regulator and Kalman filtering gains. Typically, the algorithm takes two related variables, such as position and velocity, and computes them at alternate half time-step intervals via a third-order Taylor expansion. The value of one variable at a particular time acts as the initial condition for the other variable for the next time-step. Second-order accuracies can be attained even for simple solution techniques to the equations of the related variables. Leapfrog algorithms have the further advantages of being time reversal invariant and symplectic [64, 1997].

The mathematical formulation of a leapfrog algorithm for a combined conduction and radiation heat transfer problem results in two auxiliary energy balance equations. Each equation separately considers a single form of energy transfer, namely, conduction and radiation. The solutions to these auxiliary equations are easier to obtain than the solution to the original energy balance equation. For problems involving simple geometries, analytic solutions can be found to the auxiliary energy balance equations. These solutions are then employed iteratively in the leapfrog algorithm to obtain the solution to the original energy balance equation. Effectively, conduction and radiation are considered separately and their effects are interlaced over a single time-step via the leapfrog algorithm. The use of analytic solutions in the technique leads to informative implementation and less computational demand for a given required accuracy. Even though the technique can employ analytic solutions, it is inherently numerical in nature and has with it associated stability and accuracy limits.

5.2.1.1 Mathematical Formulation

Consider a planar grey, partially transparent solid that emits, absorbs and scatters radiation. For such a case, convection, viscous dissipation and volume expansion can be omitted. The energy equation associated with this system is given by:
\[
\rho c_p \frac{\partial T}{\partial t} = k \nabla^2 T - \nabla \cdot \vec{q} + q'' \quad \text{......(5.7)} [41]
\]

where \( \rho \) is medium density, \( c_p \) is medium specific heat, \( T \) is the medium temperature distribution, \( k \) is the medium thermal conductivity and \( t \) is time. The capacity term on the left hand side of equation (5.7) describes the storage of energy within the medium. The first term on the right hand side of equation (5.7) describes the conduction within the medium and \( q'' \) allows for any internal heating of the medium. The divergence of the radiative heat flux vector \( \nabla \cdot \vec{q} \) describes the net radiative energy supplied per unit volume and is given by:

\[
\nabla \cdot \vec{q} = 4\pi \frac{\kappa}{\omega} \left[ \frac{\sigma T^4}{\pi} - S(\tau, \Omega) \right] \quad \text{......(5.8a) [41]}
\]

where \( \kappa \) is the absorption coefficient, \( \omega \) is the scattering albedo, \( \sigma \) is the Stefan-Boltzmann constant, \( \tau \) is optical depth and \( \Omega \) is solid angle. Here the source function \( S \) describes the intensity distribution throughout the medium from both emission and incoming scattering of radiation. In this case, the source function is given by the source function equation [41, 1992]:

\[
S_{\nu}(\tau, \Omega) = (1 - \omega_\nu) i_b(\tau) + \frac{\omega_\nu}{4\pi} \int_{0}^{\infty} i_s(\tau, \Omega) e^{-\tau} + \int_{0}^{\tau} S_{\nu}(\tau', \Omega) e^{-(\tau - \tau')} d\tau' \] d\Omega_i \quad \text{......(5.8b) [41]}

For the leapfrog algorithm, heat transfer via conduction and radiation are, initially, considered separately. To do this two auxiliary energy equations are formed in place of equation (5.7):

\[
\rho c_p \frac{\partial T_{\text{rad}}}{\partial t} = 4\pi \frac{\kappa}{\omega} \left[ S - \frac{\sigma T_{\text{rad}}^4}{\pi} \right] + q'' \quad \text{......(5.9)}
\]

\[
\rho c_p \frac{\partial T_{\text{cond}}}{\partial t} = k \nabla^2 T_{\text{cond}} \quad \text{......(5.10)}
\]

Equation (5.9) contains only radiative terms and equation (5.10) contains only conductive terms. The parameters \( T_{\text{rad}} \) and \( T_{\text{cond}} \) represent the temperature within the medium when radiation or conduction dominates, respectively. Thus, the capacity terms on the left hand side of equations (5.9) and (5.10) describe the temperature change of the medium due to radiation and conduction. This is not to say that the true temperature of the medium is the sum of \( T_{\text{rad}} \) and \( T_{\text{cond}} \). The true temperature is estimated by iteratively coupling the solutions to equations (5.9) and (5.10) in the leapfrog algorithm.
It is now possible to find analytic solutions to equations (5.9) and (5.10). For equation (5.9), we assume that $S$ is constant over a single iteration. Numerical integration is required to determine $S$ from the temperature profile of the medium, and therefore $S$ is updated at each new time-step. The method for determining the solution to equation (5.10) is geometry dependant. The iterative procedure by which the solutions to the auxiliary equations are interlaced is represented graphically in figure (5.9):

In figure (5.9), it is shown that the solution to equation (5.9) is updated twice for each full time-step. This assists with the interfacing of the two heat transfer processes and reduces any error due to the assumption that $S$ is constant for every half time-step. Boundary conditions and initial profiles are matched and transferred as indicated by the arrows in figure (5.9). In other words, the final profile and boundary values resulting from the solution to the auxiliary radiative equation after one time-step will be the initial and boundary conditions for the solution to the auxiliary conduction equation for the next time-step. Equation (5.9) is solved by direct integration via rearranging it into the following form:

$$\int dt = \int \frac{1}{(A - BT_{rad}^4)} dT_{rad} \ldots \ldots (5.11a)$$

Where:

$$A = \frac{\rho \omega + 4 \pi kS}{\omega p c_p} \ldots \ldots (5.11b) \quad B = \frac{4 \kappa \sigma}{\omega p c_p} \ldots \ldots (5.11c)$$

This can be done here as $S$ is considered constant and not a function of $T_{rad}$. Equation (5.11a) can be simplified via partial fractions to give:

$$\int dt = \frac{B^{1/2}}{2A^{1/2}} \int \frac{1}{A^{1/2} + T_{rad}^{1/2}} dT_{rad} + \frac{B^{1/2}}{2A^{1/2}} \int \frac{1}{A^{1/2} - T_{rad}^{1/2}} dT_{rad} \ldots \ldots (5.11d)$$
Standard integrals [102, 1999] are then used to evaluate both terms on the right hand side of equation (5.11d), resulting in the following relationship:

$$2A^{3/4}B^{1/4}(t + K) = \tanh^{-1}\left(T_{\text{md}}\left(\frac{B}{A}\right)^{1/4}\right) + \tan^{-1}\left(T_{\text{md}}\left(\frac{B}{A}\right)^{1/4}\right) \ldots \ldots (5.11e)$$

$K$ is a constant of integration and can be found from the initial conditions for each time-step. If $T_{t0}$ is the initial $T_{\text{md}}$ distribution then $K$ is:

$$K = \frac{1}{2A^{3/4}B^{1/4}}\tanh^{-1}\left(T_{t0}\left(\frac{B}{A}\right)^{1/4}\right) + \frac{1}{2A^{3/4}B^{1/4}}\tan^{-1}\left(T_{t0}\left(\frac{B}{A}\right)^{1/4}\right) \ldots \ldots (5.11f)$$

$T_{\text{md}}$ is not readily obtainable from equation (5.11f); however, it can be found very accurately with an iterative method procedure such as the Newton-Raphson algorithm. It is a more complex matter to analytically solve equation (5.10). For simple geometries, such as planar or cylindrical coordinates, it is possible to find analytic solutions in multi-dimensional cases, although, this is more difficult with the introduction of non-homogeneous boundary conditions. Thus, for more complex conduction problems, a numerical technique should be adopted. In general, the solution to equation (5.10) will require boundary conditions to be specified and evaluated correctly. For prescribed inhomogeneous and homogeneous boundary value problems the boundary conditions will be set by the problem definition and will not change during the course of the algorithm. Then the algorithm is only required to pass initial temperature profiles to the sections of the algorithm devoted to solving the auxiliary conduction and radiative equation for particular time-steps. From figure (5.9), the initial temperature profile can be seen to be the final temperature profile resulting from the solution to the auxiliary radiative equation over the previous time-step, thus:

$$T_{\text{cond}}^{(r,t_{(w-1)})} = T_{\text{md}}^{(r,t_{(2w-1)/2})} \ldots \ldots (5.12a)$$

Where $r$ is a position vector that refers to locations not on the boundaries, whose temperatures have already been specified. Similarly the initial temperature profile required for the solution to the auxiliary radiative equation is the temperature profile resulting from the solution of the auxiliary conductive equation from the previous time-step, thus:

$$T_{\text{md}}^{(r,t_{(2w-1)/2})} = T_{\text{cond}}^{(r,t_{w})} \ldots \ldots (5.12b)$$

For inhomogeneous transient boundary conditions, such as radiative boundary conditions, the algorithm should pass both boundary values and initial conditions between solutions to the auxiliary energy equations. For the case of radiative inhomogeneous boundary conditions, the solution to the radiative auxiliary equation also defines the radiative boundary conditions. Thus, for the solution to the radiative auxiliary equation, the boundary values are transient and their
values at the end of a time-step are passed to the solution of the auxiliary conduction equation, where they are considered prescribed, for the next time-steps:

$$T_{\text{cond}}(r_b, t_{(n+1)}) = T_{\text{rad}}(r_b, t_{(2n+1)/2}) \quad \ldots \quad (5.13a)$$

Where $r_b$ is a position vector that refers to locations on the boundaries. Conversely for conductive inhomogeneous boundary conditions, such as convective boundary conditions, the solution to the auxiliary conductive equation defines the boundary conditions, which are passed to the solution to the auxiliary radiative equation, where they're considered prescribed.

$$T_{\text{rad}}(r_b, t_{(2m-1)/2}) = T_{\text{cond}}(r_b, t_{(m-1)/2}) \quad \ldots \quad (5.13b)$$

### 5.2.1.2 Accuracy and Stability

The leapfrog algorithm is inherently numerical, and therefore, its accuracy is affected by changing the time and spatial steps of the algorithm. For the cases in which analytic solutions can be employed the accuracy of the leapfrog algorithm can be excellent even for large time-steps. In employing an analytic solution to equation (5.10), potentially any time-steps can be used, as the solution is not subject to any stability criteria. This would not be the case when employing a numerical technique, such as explicit finite difference, and the necessary stability criteria should be observed. In the next section, the algorithm is validated via a simple example, and its accuracy is assessed.

### 5.2.1.3 Algorithm Validation

The leapfrog algorithm is applied to a plane-layer geometry heat transfer problem where the participating medium is grey and scattering has been ignored. The grey medium has a thermal conductivity of $k$ and an absorption coefficient of $\kappa$; both of which are constant. The layer is between parallel black plates at constant uniform temperatures $T_1$ and $T_2$ which are spaced a distance $D$ apart (see figure (5.10)). This problem was originally considered in reference [60, 1962].

![Figure 5.10: Plane-layer geometry [41]](image-url)
The problem is considered, one-dimensional in \( x \), dimensionless, with normalised units such that \( D = 1 \) (therefore \( x \) varies from 0 - 1) and the reference temperature \( T_R = T_1 \). For this geometry the source function is (prime indicates dimensionless terms):

\[
S' = \frac{XK}{2} \left[ \int E_1^{ao}(x') + T_1' \int E_1^{ao} \left( \kappa(D - x) \right) + \kappa \int E_1^{ao} \left[ \kappa(x - x') \right] dx' + \int T_1''(x') E_1^{ao} \left[ \kappa(x' - x) \right] dx' \right]
\]

\[\ldots (5.14) \quad [41]\]

Where \( E_1^{ao} \) and \( E_1^{ao} \) are one-dimensional exponential, integral functions of first- and second-order respectively [41, 1992], integrated from \( \varphi = 0 \) to \( \varphi = \pi \). For this particular problem, \( T_1' = T_1/T_1 = 1/10 \) and initially the entire medium is at a temperature of \( T_1' = T_1/T_1 = 1 \). The transient temperature of the participating medium is desired. This problem is solved via the leapfrog algorithm and is compared to the solution found via an Euler finite difference algorithm as described in reference [103, 1971]. The source function (equation (5.14)) is calculated via the procedure indicated in reference [41, 1992]. Due to the simplicity of the geometry, analytic solutions are employed in the leapfrog algorithm Equations (5.9) and (5.10) in dimensionless form are:

\[
\frac{\partial T_{\text{rad}}'}{\partial t'} = S' + (q''')' - T_{\text{rad}}' \ldots \ldots (5.15)
\]

\[
\frac{\partial T_{\text{cond}}'}{\partial t'} = N \nabla^2 T_{\text{cond}}' \ldots \ldots (5.16)
\]

Where:

\[
T_{\text{rad}}' = \frac{T_{\text{rad}}}{T_1} \quad T_{\text{cond}}' = \frac{T_{\text{cond}}}{T_1} \quad t' = \frac{4 \kappa \sigma T_1^3}{\omega \rho c_p} t \quad (q''')' = \frac{q'''}{4 \kappa \sigma T_1^3} \quad S' = \frac{\pi S}{\sigma T_1^3}
\]

\[
N = \frac{\kappa \omega}{4 \kappa \sigma T_1^3}
\]

\[\ldots (5.17a-f)\]

The parameter \( N \) is the conduction-to-radiation parameter and its value denotes the relative effects of conduction and radiation. For example if \( N = 0 \) the radiative heat transfer dominates and conduction within the medium does not occur. For greater values of \( N \), conduction becomes more prevalent. Equation (5.15) is solved by direct integration with the solution:

\[
2(S' + (q'''))^{1/4} t' + K' = \tanh^{-1}\left( \frac{T_{\text{rad}}'}{(S' + (q'''))^{1/4}} \right) + \tan^{-1}\left( \frac{T_{\text{rad}}'}{(S' + (q'''))^{1/4}} \right) \ldots \ldots (5.18)
\]

For a 1D planar geometry equation (5.16) reduces to:
where \( \tau \) is the optical depth \((\tau = x \lambda)\). The boundary conditions to be applied to equations (5.18) and (5.19) are prescribed by the boundary temperatures \((T'_1\) and \(T'_2\)). However, for the case when \(N=0\), the boundary conditions become transient with the solution to the auxiliary radiative equation, but still no boundary conditions are passed as conduction now plays no role in the algorithm. Initial conditions are passed between solutions to the auxiliary equations as indicated in equations (5.12a) and (5.12b), such that:

\[
T'_\text{rad} \left( \tau, t'_{(n-1)-m} \right) = T'_\text{rad} \left( \tau, t'_{(n-1)-(2n-3)+1} \right) \quad (5.20a)
\]

\[
T'_\text{rad} \left( \tau, t'_{(2n-3)+1} \right) = T'_\text{rad} \left( \tau, t'_{(n-1)-m} \right) \quad (5.20b)
\]

The solution to equation (5.19) can be found via the method of partial solutions \([103, 1971]\), and is given by:

\[
T'_\text{cond} \left( \tau, t' \right) = \sum_n C_n \sin(\lambda_n \tau) e^{-\text{Neq} t'} + \left( \frac{T'_\text{cond} \left( D, t' \right) - T'_\text{cond} \left( 0, t' \right)}{D} \right) \tau + T'_\text{cond} \left( 0, t' \right) \quad (5.21a)
\]

Where:

\[
C_n = \frac{2}{D} \int_0^D g(\tau) \sin(\lambda_n \tau) d\tau \quad (5.21b)
\]

And:

\[
\lambda_n = \frac{n \pi}{D} \quad (5.21c)
\]

\[
g(t') = T'_\text{cond} \left( \tau, t' \right) - \left( \frac{T'_\text{cond} \left( D, t' \right) - T'_\text{cond} \left( 0, t' \right)}{D} \right) \tau + T'_\text{cond} \left( 0, t' \right) \quad (5.21d)
\]

The algorithm proceeds via the following procedure as indicated in figure (5.9):

1. The algorithm begins by evaluating \(S'\) and solving equation (5.15) over the first \(T'_\text{rad} \) half time-step, from the original initial conditions. The boundary temperatures are always fixed at \(T'_1\) and \(T'_2\).

2. The temperature profile resulting from step 1 is now the initial condition for equation (5.16) over the first \(T'_\text{cond} \) full time-step.

3. Equation (5.16) is now solved over the first \(T'_\text{cond} \) full time-step.

4. The temperature profile resulting from step 3 is now the initial condition for the solution to equation (5.15) over the second \(T'_\text{rad} \) half time-step. \(S'\) is evaluated again from the new initial condition.

5. Equation (5.15) is now solved over the second \(T'_\text{rad} \) half time-step.
6. The temperature profile resulting from step 5 is now the initial condition for the solution to equation (5.16) over the third $T'_{rad}$ half time-step. $S'$ is evaluated again from the new initial condition.

Steps 1-6 are repeated until the desired simulation time. In figure (5.11a), a series of transient temperature profiles, simulated via the Leapfrog algorithm, close to steady state, are plotted for varying values of $N$. Viskanta and Grosh [60, 1962] considered the same problem addressed here and developed a numerical iteration procedure to solve for the steady state temperature profile of the plane layer. The results of Viskanta and Grosh are also plotted in figure (5.11a) for the same values of $N$. Figure (5.11a) is an enlarged view of the area enclosed by the box seen in figure (5.11a). Figure (5.11a & b) demonstrates that the leapfrog temperature profiles are consistent with the corresponding steady state temperature profile of Viskanta and Grosh. In addition, both temperature profiles representing the $N=0$ case exhibit the temperature slip at each boundary as is normally observed for such cases [41, 1992]. Figure (5.12a & b) shows the transient behaviour of the leapfrog algorithm, along with an implicit finite difference algorithm and “truth” for the $N=0.03$ case. In figure (5.12a&b) it can be seen that as simulation time approaches infinity, all the transient algorithms converge on the steady state solution of Viskanta and Grosh, also plotted. The truth values are generated via an implicit finite difference algorithm which progresses with a very small time-step of 0.005 and has a spatial discretization of $\Delta x = 0.005$. The temperature profiles produced by this algorithm are considered the actual transient temperature of the plane layer so that transient errors can be estimated for both the leapfrog and implicit finite difference algorithms. As no exact solution is available for the transient behaviour, it is difficult to determine the true transient error. It was found that a steady state error resulted in each of the transient algorithms considered, and it appeared significantly dependant on the spatial discretization chosen. It was also found that the algorithm time-step had more effect on the transient error than the steady state error. The spatial discretization of the truth algorithm was selected such that it resulted in a steady state error of 0.1% when compared to the results of Viskanta and Grosh. This steady state error is primarily a result of the spatial discretization of the geometry resulting in errors in temperature gradient estimates and the evaluation of the source function. Figure (5.12b) is an enlarged view of the area enclosed in the box seen in Figure (5.12a) and shows the temperature profiles produced by each algorithm at simulation times of 0.4, 0.8, 1.4, 2 and 10 in non-dimensional time units. These times were chosen as they effectively demonstrate the transient temperature behaviour of the plane layer with $N=0.03$. The maximum simulation time was selected, as by this time transient temperature gradients were on the order of $10^6$ or lower for all algorithms, at which point the transient algorithm was considered to have converged on its steady state result.
In order to assess the behaviour of each algorithm's accuracy, transient simulations were conducted for a selection of time-steps and spatial discretization values. Three types of algorithm were used for these transient error tests, an implicit finite difference algorithm, a purely analytic leapfrog algorithm and implicit finite difference leapfrog algorithm. The last algorithm employs the implicit finite difference technique to solve the auxiliary conduction equation of the leapfrog algorithm. The implicit finite difference method was chosen for comparison to allow a greater range of time and spatial discretization to be explored. For most of the situations considered, an explicit finite difference method would be unstable. The behaviour of each algorithm’s transient error can be seen in figures (5.13a & b), where figure (5.13a) is the transient error for a simulation with $N=0.001$ and figure (5.13b) is the transient error for a simulation with $N=0.03$. The error is calculated by computing the absolute average difference between each algorithm and the truth algorithm at each time-steps. In both figures (5.13a & b) the transient results of the leapfrog algorithms can be seen to be much more accurate than the implicit finite difference algorithm. Furthermore, the steady state error of the leapfrog algorithms is observed to be smaller. The steady state error for each algorithm was observed to decrease when the spatial and temporal discretization were reduced.

A further error (simulation error) can be calculated to provide a single figure of merit on how accurate an algorithm was during a simulation by averaging the error plots shown in figure (5.13a & b). This was accomplished for the $N=0.001$ case and for a range of simulation time-steps with two spatial discretization values (0.01 and 0.025). The results of this analysis are shown in figure (5.14a). In this figure, it is seen that the leapfrog algorithms perform consistently better than the finite difference algorithm as the time-step employed in the simulation is increased. Furthermore, the finite difference algorithm becomes unstable after a time-step value of 0.5 where as the leapfrog algorithms remain stable and reasonably ($<5\%$) accurate, even for a single time-step from time zero to time 10. Siegel [104, 1998] notes that even implicit numerical solution techniques for coupled conduction and radiation problems tend to become unstable for excessive time-steps.

In figure (5.14a), it can be seen that as the time-step decreases for the simulation errors of algorithms with the same spatial discretization converge. This indicates that for a given spatial discretization there is a limit to the simulation accuracy that can be achieved. Yang and Gu [105, 2005] demonstrate, for implicit numerical solution methods to parabolic partial differential equations that for a certain level of spatial discretization, a minimum time-step exists for which the maximum simulation accuracy will occur. Yang and Gu also note that, in some cases, once the numerical simulation error reaches a minimum, it increases slightly if the simulation time-step is further reduced. This was attributed to the increased number of calculation steps for a reduced
time-step. This phenomenon identified by Yang and Gu [105, 2005] is observed to occur in figure (5.14a).

In figure (5.14b), the computation times required for each algorithm are plotted against time-step and it can be seen that the leapfrog algorithms consistently take approximately twice as long as the finite difference algorithm to complete a simulation for a given time-step. These figures demonstrate that the leapfrog algorithms are useful for simulations employing large time-steps in order to achieve a reasonably accurate estimate of the plane layer temperature profile in a short period of time. This is further emphasised in figure (5.14c), where the computation time required for a simulation is plotted against desired simulation accuracy for all algorithms. In figure (5.14c), it is clear that the leapfrog algorithms take less time to yield a desired simulation accuracy when compared to the implicit finite difference algorithm.

![Figure 5.11 (a): Leapfrog and finite difference plane-layer transient temperature profiles for varying values of $N$. (b): Zoom in of box in (a)](image)

![Figure 5.12 (a): Transient plane layer temperature profiles of the leapfrog, finite difference and truth algorithms at varying simulation times with $N=0.03$. (b): Zoom in of box in (a)](image)
Figure 5.13 (a): Transient error for leapfrog and finite difference algorithms with $N=0.001$ ($\Delta t = 0.1$, $\Delta t = 0.001$). (b): Transient error for leapfrog and finite difference algorithms with $N=0.03$ ($\Delta t = 0.25$, $\Delta t = 0.025$).

Figure 5.14 (a): Simulation error for leapfrog and finite difference algorithms with $N=0.001$ vs. simulation time-step. (b): Computation time of leapfrog and finite difference simulations vs. simulation time-step. (c): Required computation time of leapfrog and finite difference simulations vs. desired simulation accuracy.
5.2.1.4 Extension to Multiple Dimensions

The leapfrog algorithm can easily be extended to multidimensional problems. All that is required is a suitable solution technique to the auxiliary conduction energy equation in the geometry under study. Again, for simple geometries and circumstances, this can be an analytic solution. For the auxiliary radiation energy equation, care should be taken with the source function to ensure the appropriate exponential integral functions are employed for the given geometry [41, 1992]. To demonstrate a two-dimensional leapfrog algorithm, a two-dimensional planar coordinate system (in \(x\) and \(y\), see figure (5.15)) is selected.

![Figure 5.15: 2D planar geometry](image)

The grey medium has a thermal conductivity of \(k\), an absorption coefficient of \(\kappa\), a density of \(\rho\) and specific heat of \(c_p\), all of which are constant. The problem is considered normalised, dimensionless with \(X=1\), \(Y=1\) and the initial temperature of the plane layer (\(T_i\)) is the reference temperature (\(T_r\)) having a value of unity. As in the one-dimensional case, scattering is neglected. For this geometry, equation (5.18) remains unchanged apart from the calculation of \(S'\) which is given by:

\[
S' = (Y - y) \int_{x_0}^{x_1} T_i^0(x_0, Y) \frac{E_i^{20} \left[ k \delta_0(x_0, Y) \right]}{\delta_0^2(x_0, Y)} dx_0 \\
+ (X - x) \int_{y_0}^{y_1} T_2^0(X, y_0) \frac{E_2^{20} \left[ k \delta_0(X, y_0) \right]}{\delta_0^2(X, y_0)} dy_0 \\
+ y \int_{y_0}^{y_1} T_3^0(x, y_0) \frac{E_3^{20} \left[ k \delta_0(x, y_0) \right]}{\delta_0^2(x, y_0)} dy_0 \\
+ \kappa \int_{y_0}^{y_1} T_4^0(x', y') \frac{E_4^{20} \left[ k \delta_0(x', y') \right]}{\delta_0^2(x', y')} dx' dy' 
\]

......(5.22a)[41]

where:

\[
\delta^2 = \left( (x - x')^2 + (y - y')^2 \right)^{\alpha} \ldots (5.22b),
\]

\[
\delta(x, y) = \left( (x - x_0)^2 + (y - y_0)^2 \right)^{\alpha} \ldots (5.22c)
\]
Here $E_1^{10}$ and $E_2^{10}$ are two-dimensional exponential integral functions of first and second order respectively [41, 1992]. For this geometry equation (5.16) reduces to:

$$\frac{\partial T}{\partial t} = N \left[ \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} \right] \ldots (5.23)$$

Initially, the entire two-dimensional plane layer is at a temperature of $T_i = 1$. Then, the temperatures along boundaries 1, 3 and 4 are instantaneously dropped to $T_i = T_r = 0.5$. Thus, the boundary conditions for the simulation are the prescribed boundary values given by $T_i = T_r = T_d = 0.5$ and $T_2 = 1$. Three algorithms are employed to assess the behaviour of each algorithm's error, which is accomplished in the same manner as for the one-dimensional case. Two of the algorithms are finite difference methods; one being an implicit algorithm and the other being an explicit algorithm, and the final algorithm is a leapfrog algorithm, which makes use of an implicit solver for the auxiliary conduction equation. Yuen and Takara [106, 1986] considered the steady state two-dimensional plane layer problem and solved for the steady state temperature profile of the plane layer.

In figure (5.16a), contour plots of the two-dimensional temperature distribution for the $N=0.001$ case can be seen. These plots were produced by the ordinary implicit and the implicit leapfrog algorithm and are plotted for comparison. In figure (5.16b), the transient centre line temperature distributions of the two-dimensional plane layer, produced by the implicit, implicit leapfrog and truth algorithms, are plotted. Also plotted in figure (5.16b), for comparison, are the results reported by Yuen and Takara for the $N=0.001$ steady state centre line temperature distribution.
In figure (5.17a), contour plots of the two-dimensional temperature distribution for the $N=0.01$ case can be seen. These plots were produced by the ordinary implicit and the implicit leapfrog algorithm and are plotted for comparison. In figure (5.17b), the transient centre line temperature distributions of the two-dimensional plane layer produced by the implicit, implicit leapfrog and truth algorithms are plotted. Also plotted in figure (5.17b), for comparison, are the results reported by Yuen and Takara for the $N=0.01$ steady state centre line temperature distribution.

In figure (5.18), the transient errors of the ordinary implicit, explicit and the implicit leapfrog algorithms are plotted for the $N=0.001$ case with a time-step of 0.1. In figure (5.18), it is clear that the transient errors of the implicit leapfrog algorithms exhibit the same behaviour as seen in the one-dimensional planar case. A total dimensionless simulation time of 4 was employed as it was found to allow all algorithms to converge within at least 95% of their steady state temperature values.
Performing a similar analysis as for the one-dimensional plane layer, different simulation time-steps are used in simulations of the two-dimensional plane layer problem and the corresponding simulation errors is calculated. In figure (5.19), the simulation error for each algorithm is plotted against simulation time-step for the $N=0.001$ case. The implicit leapfrog algorithm is observed to become more accurate than the ordinary finite difference algorithms as time-step increases. Furthermore, both finite difference algorithms were observed to become unstable and very inaccurate ($>10\%$) for time-steps greater than 1 whereas the implicit leapfrog algorithm remained stable even for a single step to steady state. Nevertheless a limit in achievable accuracy is again observed. In this case, the phenomenon described by Yang and Gu is clearly visible in the implicit leapfrog algorithm. At a time-step of 0.5, the implicit leapfrog algorithm is observed to be the most accurate of the algorithms considered. With further reduction of the simulation time-step, the accuracy of the implicit leapfrog algorithm is observed to decrease to match that of the other finite difference algorithms. In order to determine if the leapfrog algorithm offers an improvement in performance, the computation time for the simulations is required. The computation times, for these simulations, versus simulation time-step is plotted in figure (5.20a). In figure (5.20b), computation time is plotted against desired simulation accuracy for each algorithm. Figure (5.20b) indicates that the implicit leapfrog algorithm is an improvement upon the ordinary finite difference algorithms as for a given desired simulation accuracy the required computation time for the implicit leapfrog method is less than that for the ordinary explicit and implicit finite difference methods. Moreover the implicit leapfrog algorithm is capable of better accuracy than the other finite difference algorithms. This improvement in accuracy is attributed to the analytic calculation of the auxiliary radiative equation in the implicit leapfrog algorithm.
5.2.1.5 Algorithm Analysis Conclusion

Employing a leapfrog algorithm to study transient coupled conductive and radiative heat transfer in participating media has been shown to be a valid, accurate and informative numerical approach for solving the energy balance equation in multidimensional geometries. The leapfrog algorithm allows analytic solutions to be employed in a numerical scheme for suitably simple geometries. A one-dimensional plane layer example demonstrated the use of analytical solutions in this capacity and the high levels of accuracy that can be attained even for large time-steps. A two-dimensional plane layer example was also included to demonstrate that the leapfrog algorithm could be employed in multidimensional problems. The main benefit of the leapfrog algorithm was demonstrated to be its ability to provide accurate transient results, performing better than standard explicit and implicit finite difference methods especially for large simulation time-steps. The accuracy of the leapfrog algorithm is of the same order as the conventional methods for spatial discretization, this can be seen in figures (5.14a) and figures (5.19) as reducing the time step for a given spatial discretization the simulation accuracies of all algorithms converge. However, the leapfrog algorithm is of a higher order of accuracy in terms of time step, such that increasing the time step for a simulation, the error increases at a slower rate in comparison to the conventional methods. It should be noted however that the accuracy of these methods depends on the spatial and transient temperature gradients specific to the problem and therefore appropriate spatial and transient discretization should be chosen to avoid large inaccuracies.
5.2.2 Fibre Optic Thermal Analysis

The thermal behaviour of the FLU fused silica fibre optic, selected for testing in chapter 4, will now be considered. Two, models of the fibre optic are simulated, one with the fibre optic coupled to the concentrator sized for the UK-DMC demonstrator STP system and the other coupled to a direct-gain receiver at high temperature. A similar analysis was conducted by Jaramillo [93, 1999], however, the attenuation spectrum of the fibre optic used in Jaramillo's analysis is more attenuating than the FLU fused silica fibre optic and Jaramillo's model is for a terrestrial environment, and therefore makes use of convection. Jaramillo's analysis is referred to in parallel with the following analysis.

5.2.2.1 Concentrator Coupled Fibre Optic Thermal Analysis

In this section the thermal behavior of the fibre optic is assessed when concentrated sunlight is focused on the tip of the fibre and allowed to traverse along its length. The geometry of the thermal model is shown in figure (5.21):

![Figure 5.21: Fibre optic cylindrical geometry.](image)

This geometry is assumed to be axis symmetric such that there are no variations in temperature rotationally about the z-axis. This model is essentially the same as the one-dimensional model shown in figure (5.5), but considered in two dimensions and both conductive and radiative heat transfer are modeled. The fibre is allowed to radiate but no convection occurs as the fibre is located in a vacuum environment. The fibre optic is heated via internal heating due to the distribution of intensity throughout the fibre. Jaramillo et al [93, 1999] use a Gaussian distribution \( G \) to describe intensity profile of the spot that is propagated through the fibre. This is consistent with the discussion concerning spot intensity distribution, discussed in chapter 4 section 4.3.2 and demonstrated by Nicolás [38, 1987]. The intensity distribution \( q \) throughout the fibre geometry is (according to Jaramillo):

\[
q_t(r, z, t) = G(r, 0, t) \exp(-\kappa z) \hat{e}_z \ldots \ldots (5.24)[93]
\]
In equation (5.24) the time variation is due to Jaramillo's model employing time varying terrestrial solar radiation as the heating mechanism. Equation (5.24) also makes an assumption with regard to the radial propagation of the concentrated radiation traversing the fibre as the exponential term, describing the diminishing of the intensity distribution (due to absorption $\lambda$ averaged across the attenuation spectrum) as a function of the z coordinate only. This assumption is valid for small rim angle concentrators, for which the propagation paths of radiation through the fibre is close to being parallel to the z axis. This is not the case for the current problem as the concentrator used has a high rim angle to accommodate a high concentration ratio. To account for the subsequent variation in light ray propagation path length a function describing the average path length within the fibre for a particular z coordinate is substituted for the z coordinate in equation (5.24). Also for the current problem a spectral dependence is added to the absorption coefficient.

For this geometry equation (5.16) reduces to:

$$\frac{\partial T'}{\partial t'} = N \left[ \frac{1}{r} \frac{\partial T'}{\partial r} + \frac{\partial^2 T'}{\partial r^2} + \frac{\partial^2 T'}{\partial z^2} \right] \ldots \ldots (5.25)$$

For this geometry equation (5.18) remains unchanged, apart from the calculation of the source function which is geometry dependant. The calculation of the source function is dependant on the axis symmetric nature of the temperature distribution. For a given coordinate in $r$ and $z$ the temperature at that point is the same for all $\theta$, see figure (5.22).

The distance between two points ((z, r) and (z*, r*)) in the $r$, $z$ plane is denoted $\Delta$. If the angle $\theta$ is varied for the dummy coordinate (z*, r*) then $\Delta$ also varies, such that it has an average value of:

$$\bar{\Delta} = \frac{1}{\theta} \int_0^{\pi} \Delta d\theta = \sqrt{r^2 + (r^*)^2 + (z - z^*)^2} \ldots \ldots (5.26)$$

Thus the source function can now be written as:

$$I' = \frac{\kappa}{2} \int_0^{\pi} \int_0^l \frac{T'(z^*, r^*, t')^4}{\Delta^2} dz^* dr^* \ldots \ldots (5.27)$$
The parameters describing the model for the FLU silica fibre optic and concentrator heat transfer simulation are shown in table (5.7):

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core diameter (mm)</td>
<td>1</td>
<td>Fibre length (m)</td>
<td>1</td>
</tr>
<tr>
<td>Cladding thickness (mm)</td>
<td>0.3</td>
<td>Fibre numerical aperture</td>
<td>0.66</td>
</tr>
<tr>
<td>Buffer thickness (mm)</td>
<td>0.8</td>
<td>Fibre fresnel reflectivity</td>
<td>0.04</td>
</tr>
<tr>
<td>Core thermal conductivity (Wm(^{-1})K(^{-1}))</td>
<td>1.71</td>
<td>Concentrator rim angle</td>
<td>40°</td>
</tr>
<tr>
<td>Cladding thermal conductivity (Wm(^{-1})K(^{-1}))</td>
<td>0.23</td>
<td>Concentrator diameter (mm)</td>
<td>105</td>
</tr>
<tr>
<td>Buffer thermal conductivity (Wm(^{-1})K(^{-1}))</td>
<td>0.35</td>
<td>Concentrator Concentration ratio</td>
<td>11100</td>
</tr>
<tr>
<td>Core density (kg/m(^3))</td>
<td>2200</td>
<td>Concentrator focal length (mm)</td>
<td>72.1</td>
</tr>
<tr>
<td>Core specific heat (Jkg(^{-1})K(^{-1}))</td>
<td>1026</td>
<td>Solar constant (w/m(^2))</td>
<td>1370</td>
</tr>
<tr>
<td>Initial temperature (°C)</td>
<td>21</td>
<td>Concentrator reflectivity</td>
<td>0.83</td>
</tr>
</tbody>
</table>

The attenuation spectrum for the simulation is as plotted in figure (5.2). The temperature profile of the fibre after a vacuum heating period of 2 hours is shown in figure (5.23). It should be noted that although the fibre has been heated for two hours, it has not reached thermal equilibrium and will get hotter with continued heating. From figure (5.23) it can be seen that there is not much variation in temperature with radius, which is to be expected given the small dimension of the fibre in that direction giving a small biot number. The variation of the temperature along the length of the fibre is much more pronounced and is plotted in figure (5.24) for clarity. The peak temperature of the fibre is 148°C (421K), which is less than the temperature limit set by the fibre manufactures of 460K and is consistent with the one-dimensional analysis conducted in section 5.1.2 (see figure (5.7)). Furthermore, it should be noted that when the simulation parameters are changed to those used by Jaramillo et al, the temperature profile results are consistent with those of Jaramillo et al, providing confidence in the results. This peak temperature represents a worst-case scenario, as no conductive contact between the fibre and its surrounding supporting structure is modelled, which would undoubtedly cause the fibre to be cooler. If the fibre were to be used for very long heating period (up to 10 hours, as in the case of the HLEO thermal storage STP system

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discussed in chapter 3), other means of cooling the fibre would be necessary, such as radiating fins. However, for the UK-DMC STP demonstration system, which will require less than an hour's vacuum heating time, there should be no danger of the fibre over heating.

![Temperature profile of fibre for 2 hour vacuum heating](image1)

![Temperature profile of fibre centre for 2 hour vacuum heating](image2)

5.3 Fibre Optic Cable Testing

The fibre optic cable selected for this research is Polymicro’s FLU Teflon clad silica core fibre optic cable. This fibre was chosen due to its unusually large NA. Polymicro supplied a 50m order of the FLU cable sectioned into ten 2m lengths, one 10m length and one 20m length. The tips of each fibre were machine polished to ensure good coupling between concentrator and fibre.
In this section a series of experiments are described which investigate:

- Variation in fibre transmission with length.
- Confirmation of fibre optic attenuation spectrum.
- Investigation of exposure of FLU fibre to extreme temperatures.

### 5.3.1 Fibre Transmission Variation with Fibre Length

Fibre transmission testing was conducted during the summer of 2005. The purpose of this testing was to study the effect of fibre length on transmission efficiency and to confirm the linearity of the addition of power from multiple fibres. Although the FLU fibre has a very low attenuation over the solar spectrum (expected nominal attenuation ~0.04 dB/m), transmission of high intensities can increase loss mechanisms such as photoluminescence and non-perfect total internal reflection. The tests were conducted using the Sun as a power source. A Losmandy solar tracking tripod mount was used to orientate a 14 cm diameter concentrator to point at the Sun. A two-axis micrometer stage was employed to locate the fibre tip at the concentrator’s focal point. Delivered fibre power was measured using a LOT-Oriel thermopile sensor, having a response over a broad range of wavelengths. A pyroheliometer is used to measure local solar intensity. The equipment set up is depicted in figure (5.25):

![Fibre transmission tests, summer 2005.](image)

Fibre lengths of 2m, 10m and 20m were used to analyse the effects of fibre length on transmission efficiency. These results are plotted in figure (5.26). On swapping over different lengths of fibre optic cable the focal position was maintained.
The transmission powers observed in figure (5.26) are the best transmission efficiencies that could be attained with the available equipment. The main source of power transmission loss is attributed to the pointing accuracy of the open Losmandy Sun tracking mount being unable to exactly align the fibre with the concentrator focal point in a dynamic environment. From figure (5.26) we can see that there is a significant decrease in transmitted power for relatively small increases in length.

An exponential trend line was fitted to the data in figure (5.26), using Microsoft Excel, such that a nominal attenuation value could be ascertained. The R-square$^{26}$ value of the exponential fit with the observed data was 0.97. It was expected that the change in transmitted power with fibre length would obey an exponential relation such that:

$$P = P_0 e^{-Kx} \quad \text{(5.28)}$$

where $P$ is the radiative transmitted power over the distance $x$, $P_0$ is the incident radiative power on the fibre tip and $K$ is the nominal attenuation of the FLU fibre optic. The equation of the fitted exponential trend line is:

$$P = 3.6e^{-0.031x} \quad \text{(5.29)}$$

Equation (5.29) was then used to calculate the observed nominal attenuation, based on the assumed exponential relation, to be 0.26 dB/m. This is significantly different to the expected nominal attenuation of -0.04 dB/m, which was calculated via integration of the manufacturer provided FLU fused silica fibre optic attenuation spectrum over the solar spectrum from 300nm to 2000nm. It was found, when fitting a power series trend line to the experimental data, that a slightly better match occurred (R-square = 0.99). This power series trend line is shown in figure (5.26) and its equation is:

$$P = 4.2x^{-0.24} \quad \text{(5.30)}$$

\footnote{The R-square value is a statistical parameter who’s value describes the goodness of fit between a model and observed data. An R-square value of 1 means a perfect fit between model and observed data.}
If a power series relationship between the transmitted power and fibre length is assumed then the attenuation occurring in the fibre will vary as a function of fibre length. The relationship between fibre attenuation and fibre length was calculated for equation (5.30), extrapolated, and is shown in figure (5.27):

Figure (5.27): Extrapolated variation in fibre attenuation with fibre length for the "power" series trend line, the "exponential" series trend line and the expected nominal attenuation.

Figure (5.27) shows that if a power series relationship is indeed the case, then the attenuation of the fibre will decrease as light propagates through the fibre. It can also be seen in figure (5.27) that the attenuation in the fibre reduces to the expected nominal attenuation of 0.04 dB at a fibre length of around 50 m. This type of behaviour is consistent with the behaviour of fibre optic "light leakage" as discussed in the literature. For example Feuermann et al [55, 2002] observe a decrease in fibre transmission with light incidence angle, suggesting that transmission loss through a fibre will be more significant initially but as light progresses further along the fibre observed transmission loss for a particular section of the fibre will decrease. However, this argument is based on a very small amount of experimental data and the difference between the goodness of fit (R-square) for the exponential and power series trend lines is small. If the variation of transmission power with length is indeed an exponential one, then the nominal attenuation of the FLU fibre is much higher than expected. It was therefore decided to independently measure the attenuation spectrum of the FLU fibre. Unfortunately, only fibre lengths of 2, 10 and 20 m were available to perform these tests. If these tests were to be repeated a greater range of FLU fibre optic lengths would likely produce a more definitive result.

Assuming the exponential trend line is true, shown in figure (5.26), the power into the fibre can be estimated by extrapolating the trend line to zero length. Then comparing these values to the known power into the fibre, based on local solar intensity and concentrator efficiency, the coupling efficiency can be estimated. From the exponential trend line the power at zero length is estimated.
to be 3.6 W. The known power into the fibre is 5.2 W (local solar intensity 750 W/m²) resulting in a ~70% coupling efficiency.

A connector was manufactured for the fibre optic to allow it to be accurately positioned along the focal axis of the concentrator, see figure (5.28). An alumina ceramic tube is fed through an aluminium outer support, which is accurately aligned with the focal axis. The fibre is bonded to the alumina tube with high temperature epoxy resin. The connector can be easily removed from the concentrator via threaded bearing. The alumina tube having a high stiffness supports the fibre to the focal spot along the focal axis of the concentrator with a tolerance of ± 0.1 mm (this is the tolerance determined in chapter 4 section 4.3.3). This connector was employed to position the fibre along the focal axis of the Cassegrain concentrating unit in the 2005 summer transmission tests. Difficulty was initially experienced in aligning the fibre tip with the focal point. This was due to error in the machining of the aluminium support for the alumina tube. A second aluminium support was fabricated that more accurately aligned the fibre tip with the focal point. The Cassegrain concentrating prototype was then used to concentrate solar energy into a 2 m long FLU fibre optic. A 3.2 W power transmission was observed demonstrating an end-to-end efficiency of 50%.

Figure 5.28: Fibre optic connector for prototype Cassegrain concentrator unit.
The linearity of power addition for multiple fibre optics was confirmed by employing the two parabolic concentrators each coupled to a fibre optic cable. Their separate transmitted power added linearly to produce a total output power of 6.7 W as measured by the thermopile detector.

A further transmission test was conducted in December 2005, aimed at determining the effect of fibre optic cable bend radius on transmission. Using a solar simulator, light was concentrated into the FLU fibre optic cable via a 14 cm diameter parabolic mirror. No loss in transmitted power was observed at bend radii as small as 30 mm, for a 180° bend. However significant elastic energy was observed in the fibre at this bend radius, indicating a need to mechanically support the fibre for such tight bends.

5.3.2 Fibre Optic Cable Attenuation Spectrum Analysis

In light of the fibre transmission experiments conducted in the summer of 2005 it was deemed necessary to independently measure the attenuation spectrum of the FLU fibre optic cable and compare it to the manufacturers specification. To do this the fibre was taken to Surrey Universities Advanced Technology Institute (ATI), where an optical spectrum analyser (OSA) was used to measure the spectrum of a calibrated white light source, which was transmitted through the fibre optic, see figure (5.29).
A technique known as the 'cut back method' was used to calculate the fibres attenuation spectrum. This involves measuring the transmitted spectrum of the calibrated white light source for two lengths of fibre optic cable. Comparing the two spectrums allows the calculation of the attenuation over the difference in distance between the two lengths of fibre. Two metre and twenty metre fibre optic lengths were used to maximise the difference in transmitted spectrum, for the available fibre lengths. The OSA has a wavelength range of 350 nm to 1,750 nm allowing the measurement of Ultraviolet, Visible and near Infra Red regions of the fibre attenuation spectrum. This is also the range over which the solar spectrum is most prominent. An optical bench was employed to ensure that the two fibres were the same distance away from the calibrated white light source. Figure (5.30) compares the manufacture attenuation spectrum and that calculated via the cut back method. The two attenuation spectrums, shown in figure (5.30) demonstrate similar features. These include the observed water absorption peak at ~1,400 nm, attributed to the OH ion, and a relatively large increase in attenuation towards the UV end of the spectrum. Difficulty
Fibre Optic Applications in Solar Thermal Propulsion Systems

was experienced in achieving a valid attenuation spectrum for the fibre, as the difference between the measured transmission spectrums of the two fibres was very small. The sensitivity of the fibre attenuation spectrum measurement was limited by the length difference between the two fibres (18m) and is the most likely source of error. A longer fibre optic cable would have allowed a better measurement of the fibre attenuation spectrum. However, in light of these results and the experimental difficulty in measuring the attenuation spectrum, it was clear that the large nominal attenuation (0.26 dB/m) observed in the summer 2005 tests for increasing fibre length is due to other loss mechanisms such as non-perfect total internal reflection and photoluminescence.

5.3.3 Fibre Optic Attenuation Spectrum Thermal Analysis

When the fibre optic is coupled to the receiver it will be exposed to very high temperatures (>1,000 K). The FLU fibre optic is rated, by the manufacturer, to a maximum temperature of 160°C. Thus it was deemed necessary to investigate the effects of high temperatures on the spectral and physical properties of the FLU fibre optic. Subsequently a 20 m length of the FLU fibre was again taken to the Surrey University ATI were it was fed through a cylindrical furnace with one end coupled to a white light source and the other to an OSA, as shown in figure (5.31).

The experiment concept was to perform a destructive fibre test, over a relatively small length of fibre (40 cm), and monitor changes in the transmitted white light source intensity while the furnace heats the fibre. The fibre temperature was monitored by a thermocouple attached to the fibre surface with high temperature epoxy resin. The furnace has a 0.4m long cylindrical heating area through which the fibre was passed. The fibre was heated up to a temperature of 1,000 K and
a white light source spectrum was transmitted through the fibre optic cable measured at 290 K, 440 K, 550 K and 1,000 K. Intermediate fibre temperatures were difficult to obtain due to the rapid heat up of the furnace. The fibre was left at a temperature of 1,000 K for 40 minutes to ensure the fibre reached thermal equilibrium with the furnace, after which the fibre was then allowed to cool. During cooling the white light source spectrum was again monitored at fibre temperatures of 800 K, 600 K, 550 K and 400 K. The furnace was a massive source of IR radiation that dominated the transmitted white light source spectrum in the IR regions. Therefore only the UV and visible regions of the spectrum up to 900 nm were measured. Each spectrum (corresponding to a particular fibre temperature) was compared to a room temperature white light source spectrum taken before fibre heating. These results are plotted in figure (5.32):

Figure 5.32: (a) Fibre attenuation increase during heat up. (b) Fibre attenuation decrease during cool down.
From figure (5.32a) we can see that there is an increase in fibre attenuation for increasing fibre temperature. The plot for a fibre temperature of 440 K shows the increase in attenuation as the fibre just exceeds its maximum temperature limit (433 K), and it is clear that significant attenuation is already occurring, especially towards the UV end of the spectrum. Above this temperature, the plots for a fibre temperature of 550 K and 1,000 K indicate much larger attenuation. An explanation for this increase in attenuation is possibly due to the complete breakdown of the optical properties of the fibre buffer and cladding during the fibre heating, and also the exposure of the fibre core to a high temperature atmosphere. Upon cooling we can see that the attenuation recovers to an extent, in figure (5.32b). The loss of the cladding and buffer explains why the attenuation spectrum does not return to its original state. Interestingly there are troughs in each spectrum at 545 nm and 613 nm, indicating that transmission of these wavelengths is less affected by the increase in temperature and loss of cladding. After removal of the fibre from the furnace, the fibre cores physical properties appeared unaffected, however, the fibre buffer and cladding had become charred and crumbled easily away from the fibre core, as shown in figure (5.33).

![FLU fibre Teflon cladding after heating.](image)

Figure 5.33: FLU fibre Teflon cladding after heating.

Although the silica fibre core appeared to be unaffected, it should be considered that on approaching the maximum exposure temperature of 1,000 K the optical properties of the silica core itself were beginning to deteriorate, such as micro bends and scratches on the fibre surface. In an air environment with these excessive temperatures, it is also possible for contaminants to have entered the core fibre material. Conducting this same experiment in vacuum would likely be able to resolve such issues, however, it is clear that tight thermal control of the FLU fibre would be required and a temperature safety margin established on account of increased attenuation close to the maximum fibre temperature limit. Comparing the attenuation spectrums of the fibre before and after heating, a 2.6 dB increase in attenuation is observed, on average attributed to a 0.4 m loss of cladding. This stresses the need to minimise the temperature to which the fibre is exposed.
A silica core, silica clad fibre optic would perform better at these elevated temperatures, however this would reduce the attainable concentration ratio of the system, as currently, a silica-silica fibre is not yet available with such a large NA as the FLU fibre. Given that only small lengths (<5 cm) of fibre optic will be exposed to the high temperatures of the STP receiver suggests that much smaller attenuation would result for a real STP system. However, any attenuation due to increased fibre temperature should be minimised. A receiver thermal design that considers this is discussed in the next chapter.

5.4 Chapter Summary and Conclusions

5.4.1 Chapter Summary

In this chapter the investigation of selecting a suitable fibre optic cable for a fibre augmented solar thermal propulsion demonstration system has been described. Initially, the properties of a suitable fibre were identified and the causes of possible intrinsic and environmental fibre attenuation were reviewed. A selection of candidate fibres is identified and the FLU fibre optic of Polymicro is selected for practical testing. Then, a novel leapfrog algorithm is described which is concerned with coupled conduction and radiation heat transfer problems. This algorithm was shown to be more accurate, stable and less computationally demanding than current solution methods for a given desired accuracy. This algorithm was employed to estimate the temperature profile of a fibre optic subjected to heating while transmitting concentrated solar energy. Finally, a series of practical tests were performed to assess the FLU fibres efficiency and properties.

5.4.2 Chapter Conclusions

From the analysis and discussion in this chapter the following conclusions were made:

- For the application of transmitting highly concentrated solar energy through fibre optics, the literature suggests that fused silica core step index fibres are most suitable.

- To minimise intrinsic fibre attenuation a fibre must have a low hydroxyl ion content and low impurity content.

- To minimise external fibre loss, a fibre must be:
  - Subjected to physical bend radii above manufacturer specific limit.
  - The fibre must be protected from stress and strain and should be sheathed in Kevlar fibres.
  - The fibre length should be minimised for any given application.
• Long duration (~5 years) radiation exposure in a low Earth orbit space environment will result in a tolerable increase in fibre attenuation (~1% decrease in efficiency).

• Short duration (~1 month) radiation exposure in a medium Earth orbit to Geosynchronous orbit space environment will result in a tolerable increase in fibre attenuation (~1% decrease in efficiency).

• Most currently available high numerical aperture fused silica fibre optics (NA ~0.66), have a low temperature tolerance due to the cladding material having a low temperature tolerance.

• Two thermal models (1-D and 2-D) of a single FLU fused silica fibre optic, accepting and transmitting concentrated solar energy from a 105 mm diameter concentrator mirror, predicted consistent peak fibre temperatures (~440 K) after a 2 hour heating period. The manufacturer’s peak temperature limit for the FLU fibre is 433 K, indicating that for mission applications requiring less than 2 hours of receiver heating, the FLU fibre can be safely employed.

• The nominal attenuation of the FLU fibre (observed nominal attenuation ~0.26 dB/m) was found to be higher than expected (expected nominal attenuation ~0.04 dB) during fibre testing when transmitting concentrated solar energy. An independent measurement of the attenuation spectrum of the FLU fibre was in agreement with that provided by the manufacturer. An explanation of this behaviour is the presence of the “light leakage” phenomenon reported in the literature or poor coupling efficiency (estimated to be ~70%). Further tests are required to provide a more definitive conclusion. However, if this phenomenon is occurring, then perhaps if the concentration of the concentrator was reduced, observed nominal attenuation would also be reduced.

• Extreme temperature exposure tests of the FLU fibre optic demonstrated significant increase in attenuation with increased fibre temperature. This indicates that if this fibre is to be used in a fibre augmented solar thermal propulsion system it would need to be protected from extreme temperature gradients. The fused silica core material of the FLU fibre survived these extreme temperature tests (up to 1,000 K), however the cladding and buffer material of the FLU fibre did not. If the cladding material of the fibre were silica then it would be able to survive these extreme temperatures, like the core material did. However, current commercially available fibres with silica cladding do not have as high a numerical aperture as the Teflon clad FLU fibre.
Chapter 6

6 STP Receiver Investigation

In chapter 3 the two types of STP cavity receiver were identified, namely, direct-gain and thermal-storage. As discussed in chapter 3 a STP demonstrator system would most likely be placed on a low earth orbit spacecraft. In a low earth orbit, orbital periods are short (~90 minutes) with a high percentage of the orbit typically in eclipse. In such a situation a direct-gain receiver would be more suitable than a thermal-storage receiver due to the lengths of time typically required to heat these receivers. A direct-gain receiver would be capable of quickly attaining high temperatures and subsequently demonstrating good performances at low thrusts. Despite this, the discussion in this chapter will cover both direct-gain and thermal storage receivers although detailed modelling and experimental work is confined to a direct-gain scale receiver.

6.1 STP Receivers

This section discusses the theory and background of both direct-gain and thermal-storage style receiver and discusses the preliminary design of both, based on the mission requirements of chapter 3.

6.1.1 Fundamental Theory of Propulsion and Heat Transfer

Rocket propulsion is the acceleration of matter, stored within a vehicle, to provide a force that moves the vehicle in a given direction. Spacecraft propulsion systems can be separated into three categories:

- Launch vehicle propulsion: This type of propulsion provides the spacecraft with the necessary velocity to leave the Earth's surface and go into orbit.
- Orbital manoeuvre propulsion: This type of propulsion allows the spacecraft to move from one orbit to another.
- Attitude control propulsion: This type of propulsion allows the spacecraft to control its orientation.
Solar thermal propulsion is associated mainly with the second of these categories, although the potential for a solar thermal attitude control system is highlighted in this work. STP is typically considered as a chemical monopropellant propulsion system. A simple diagram of a STP receiver/thruster is shown in figure (6.1):[15, 1992]

![Figure 6.1: STP receiver.](image)

In figure (6.1) a monopropellant is delivered to a heat exchanger chamber where solar energy is used to heat the propellant to high temperatures. The hot propellant gas produced then expands and accelerates through a throat and nozzle to supersonic speeds and is then exhausted. Two principal parameters concerning propulsion system design are thrust and specific impulse. Thrust ($T$) is the amount of force applied to the spacecraft, produced by the expulsion of propellant from the propulsion system:

$$T = \dot{m}u_e + (P_e - P_a)A_e \ldots (6.1)$$

Where $\dot{m}$ is the mass flow rate, $u_e$ is the propellant exhaust velocity, $P_e$ is the propellant gas pressure at the nozzle exit, $P_a$ is the ambient pressure (zero in space) and $A_e$ is the nozzle exit area.

In equation (6.1) on the right-hand-side the first term is referred to as the momentum thrust and the second term is referred to as the pressure thrust. Maximum thrust is obtained when propellant exit pressure is equal to the ambient pressure; this condition is referred to as optimal expansion. Although under this condition the contribution of the pressure thrust is zero the actual thrust is maximised. This is due to the exhaust velocity having an inversely proportional dependency on the propellant exit pressure for given propellant and chamber temperature, as seen in equation (6.2).

$$u_e = \sqrt{\frac{2\gamma RT_e}{(\gamma-1)M}[1 - \left(\frac{P_e}{P_a}\right)^{\frac{\gamma-1}{\gamma}}]} \ldots (6.2)$$

[134]
Where $T_c$ is the temperature within the chamber, $R$ is the universal gas constant (8,314.3 J-kmol$^{-1}$K$^{-1}$), $\gamma$ is the ratio of specific heats$^{27}$ and $M$ is the propellant molecular weight. However, it would be impossible in vacuum to expand the exhaust gas to the ambient pressure of zero at the nozzle exit plane, therefore the term on the far right hand side of equation (6.1) must be taken into account and results in a decrease in attainable thrust. The thrust-to-weight ratio is given as the ratio of maximum thrust to the initial mass of the vehicle [2, 2005].

From equation (2.1) we can therefore see that the specific impulse can be related to these parameters in the following equation:

$$I_{sp} = \frac{u_e}{g} = \frac{2\gamma R T_c}{(\gamma-1)g T} \left[ 1 - \left( \frac{P_e}{P_c} \right)^{\gamma-1} \right] \ldots \ldots (6.3)$$

$I_{sp}$ is a measure of the energy content of the propellants and how efficiently it is converted into thrust [15, 1992], measured in units of seconds. From equation (6.3) it can be seen that larger $I_{sp}$ values can be obtained not only by reducing the exhaust pressure, but also by maximising the ratio $T_c/M$. This explains why past STP system concepts have focussed on using a very low molecular mass propellant (such as hydrogen, $M = 2$ g/mol) heated to very high temperatures ($>2,000$ K). From equation (6.3) the ideal performance of an ammonia ($M = 17.03$, $\gamma = 1.14$) propulsion system can be derived, as stated in chapter 3, to be 407 s assuming a chamber temperature of 2,000 K. STP can be shown to fulfil a gap within the typical performance space of existing propulsion systems, as demonstrated in figure (6.2):

![Figure 6.2: performance of various classes of propulsion system including STP, compiled from various references [2,15,33]](image)

$^{27}$ The ratio of specific heats refers to the ratio of the propellants specific heat capacity at constant pressure with that at constant volume. This ratio typically has a value between 1.1 and 1.7.[9]
In figure (6.2) it can be shown how STP can be seen to compare to conventional propulsion systems in terms of thrust to weight ratio and specific impulse. This is not to say that another type of propulsion system can also be designed to bridge the gap between chemical and electric propulsion, but serves to demonstrate that STP can meet the propulsive requirements as shown in the figure.

Heat must be transferred to the propellant via a heat exchanger. Heat transfer can occur via three separate mechanisms, namely conduction, convection and radiation.

Conduction is characterised by the Fourier law:

$$Q_x = -kA \frac{dT}{dx} \quad \ldots \ldots \ldots \ldots \ldots \ldots \ldots (6.4)$$

This associates heat transfer rate ($Q_x$ (W)) in the $x$ (m) direction to the temperature gradient in that direction. $A$ (m$^2$) is the area through which the heat passes, and $k$ is a constant of proportionality specific to the medium through which the heat passes though, defined as the thermal conductivity of the medium (W/m-K). Conduction describes how heat transfer occurs within the receiver and receiver insulation.

Convective heat transfer occurs as a consequence of fluid motion over a solid object, and is characterised by Newton's law of cooling:

$$Q = h_{\text{conv}}A(T_f - T_b) \quad \ldots \ldots \ldots \ldots \ldots \ldots \ldots (6.5)$$

$Q$ represents the heat transfer rate between a body at temperature $T_b$(K) and a fluid at temperature $T_f$(K). The convection heat transfer coefficient ($h_{\text{conv}}$ (W/m$^2$-K)) describes the type of fluid flow regime, such as laminar or turbulent fluid flow. In complex systems $h_{\text{conv}}$ can only be determined experimentally. Convection describes how heat transfer occurs between the receiver and the propellant.

Radiation is the only heat transfer mechanism that can occur through a vacuum. The Stefan-Boltzmann law gives the radiative heat transfer rate ($Q$) between a body, at temperature $T_b$(K) and its surroundings, at temperature $T_m$ as:

$$Q = \varepsilon A \sigma (T_b^4 - T_m^4) \quad \ldots \ldots \ldots \ldots \ldots \ldots \ldots (6.6)$$

with $\varepsilon$ defined as the bodies’ emissivity and $A$ as the bodies’ radiating area. The constant $\sigma$ is the Stefan-Boltzmann constant ($\sigma = 5.6697 \times 10^{-8}$ W/m$^2$-K$^4$). The radiative heat exchange from one body to another, at temperatures $T_1$ and $T_2$ respectively is given by:

$$Q = F A \sigma (T_1^4 - T_2^4) \quad \ldots \ldots \ldots \ldots \ldots \ldots \ldots (6.7)$$
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where the factor $F$ accounts for the geometrical arrangement of the bodies radiating surfaces and their respective emissivities. In some cases equation (6.7) can be linearised, and a radiative heat transfer coefficient can be defined as $h_r = F4\sigma T^4$. Radiative heat transfer describes how heat is transferred from the incident solar radiation to the receiver and from the receiver/insulation to space.

6.1.2 Receiver Materials

Materials for a direct-gain receiver are dependant on the target propellant temperature, and the thermal properties of the candidate material at that temperature. For the purposes of achieving specific impulses in the range of 300 to 400s, a target temperature range for the cavity receiver is set at 1,400-2,600 °C. Ideally the receiver material should also have high thermal conductivity, a moderately low heat capacity, be chemically stable with the propellant at receiver temperature and be non-brittle over the range of operating temperatures. Thereby allowing the receiver to achieve a high temperature, quickly and efficiently transferring heat to the propellant. Suitable materials are typically high temperature metals, which are generally referred to as refractory metals. Table (6.1) suggests some candidate direct-gain receiver materials.

Table 6.1: Candidate Direct-gain receiver materials [107,108]

<table>
<thead>
<tr>
<th>Candidate Material</th>
<th>Haynes Stainless Steel</th>
<th>Chromium</th>
<th>Molybdenum</th>
<th>Rhenium</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melting temperature (K)</td>
<td>1,600</td>
<td>2,133</td>
<td>2,890</td>
<td>3,453</td>
</tr>
<tr>
<td>Thermal conductivity (W-m$^{-1}$K$^{-1}$)</td>
<td>8.9</td>
<td>69.1</td>
<td>138</td>
<td>39.6</td>
</tr>
<tr>
<td>Specific heat capacity (J-g$^{-1}$K$^{-1}$)</td>
<td>0.397</td>
<td>0.461</td>
<td>0.255</td>
<td>0.138</td>
</tr>
<tr>
<td>Density (kg-m$^{-3}$)</td>
<td>8.030</td>
<td>7.200</td>
<td>10.220</td>
<td>21,040</td>
</tr>
</tbody>
</table>

From table (6.1) we can see that both Molybdenum and Rhenium would be suitable for the target temperature range. Molybdenum stands out with its exceptionally high thermal conductivity.

For thermal-storage thrusters the materials choice is dependant on the method of heat storage of which there are several:

- Sensible heat storage: Employs high heat capacity materials to store heat over a single phase. Energy densities on the order of 1 MJ/kg are achievable.

- Latent heat storage: Uses materials phase change to store heat. Energy densities on the order of 4 MJ/kg are achievable.
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- Thermochemical heat storage: Employs a chemical reaction within the receiver material to generate heat. Reaction must be reversible. Energy densities on the order of 1 MJ/kg are achievable.

The simplest of these heat storage approaches is sensible heat storage and coupled to its reasonable energy density is deemed most suitable for this application. Kennedy suggests some candidate sensible heat storage receiver materials, listed in table (6.2).

Table 6.2: Candidate Thermal-storage receiver materials [1,108]

<table>
<thead>
<tr>
<th>Candidate Material</th>
<th>Graphite</th>
<th>Alumina</th>
<th>Boron carbide</th>
<th>Boron nitride</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melting temperature (K)</td>
<td>3,900</td>
<td>2,300</td>
<td>2,700</td>
<td>3,273</td>
</tr>
<tr>
<td>Thermal conductivity (Wm(^{-1})K(^{-1}))</td>
<td>24</td>
<td>33</td>
<td>30</td>
<td>17</td>
</tr>
<tr>
<td>Specific heat capacity (Jkg(^{-1})K(^{-1}))</td>
<td>2.091 (at 2,000 K)</td>
<td>1.360 (at 1,600 K)</td>
<td>2.511 (at 2,000 K)</td>
<td>1.988 (at 2,000 K)</td>
</tr>
<tr>
<td>Density (kgm(^{-3}))</td>
<td>2,100</td>
<td>3,980</td>
<td>2,520</td>
<td>2,270</td>
</tr>
</tbody>
</table>

Graphite offers a high melting temperature plus a competitive heat capacity at high temperatures, plus it is cheap and easy to machine. Its main drawback is the possibility of it reacting with the propellants at high temperature. Alumina although having the highest thermal conductivity, also has the lowest melting temperature that is much lower than the maximum target receiver temperature range. Boron carbide offers the highest heat capacity allowing for a smaller cavity receiver, however it is also susceptible to reactivity with the propellant. Finally Boron nitride is the most resistant to chemical attack plus it has the highest melting temperature. Kennedy suggests that a Boron carbide receiver coated with a Boron nitride layer to protect against chemical attack would be a suitable combination [1, 2004].

6.1.3 Receiver Power Loss and Insulation Materials

The peak receiver temperature \(T_r\) is heavily dependant on the concentration ratio \((A_c/A_r)\) in this case the total area of the concentrator system \((A_c)\) divided by the receiver aperture \((A_r)\) of the concentrator system. This is demonstrated by the relation described by Kreider [109, 1979]:

\[
T_r = \left[ \frac{(\eta_\nu - \eta_c)}{\sigma e} \right]^{\frac{1}{2}} \left[ \frac{A_c}{A_r} \right]^{\frac{1}{2}} \cdots \cdots \cdots (6.8)[109]
\]
Where $\eta_r$ is the optical efficiency, $\eta_c$ is the collection efficiency, $I$ is the incident intensity, $\sigma$ is the Stefan-Boltzmann constant and $\varepsilon$ is the receiver emissivity.

![Graph](image1)

**Figure 6.3:** Theoretical peak receiver temperature vs. concentration ratio of concentrator system (taken from equation (6.8))

If the concentrator system area is made from six 105 mm diameter parabolic concentrators, as for the UK-DMC STP demonstrator system then the peak receiver temperature can be compared to the receiver aperture diameter.

![Graph](image2)

**Figure 6.4:** Theoretical peak receiver temperature vs. receiver aperture diameter, for UK-DMC demonstrator concentrator system.
From figure (6.4) it is clear that small changes in aperture diameter can result in large drops in temperature. It is, therefore, important to ensure that the fibre optic diameter matches as closely as possible to the individual concentrator image size.

A hot (>2,000 K) receiver if un-insulated will radiate a large amount of power into space (as indicated in equation (6.6)). When the receiver reaches a steady state temperature during heating, the power that the receiver is absorbing will equal that which it is losing by radiating to the environment. An insulation package around the receiver can reduce this power loss for a given receiver temperature, thereby increasing the obtainable steady state temperature. There are two methods of insulating a hot receiver, these being conductive and radiative insulation. With conductive insulation a material capable of withstanding the high receiver temperatures and possessing a low thermal conductivity surrounds the receiver thus reducing the temperature of the external surface exposed to space, therefore reducing the power radiated to space for a given receiver temperature. This is demonstrated via equation (6.9).

\[ Q = \frac{2\pi k L (T_2 - T_1)}{\ln\left(\frac{r_2}{r_1}\right)} \]  

Figure 6.5: 1-dimensional steady state heat flux through a thick walled cylinder

Equation (6.9) describes the one-dimensional steady state heat flux through a thick walled hollow cylinder. Where k is the thermal conductivity of the cylinder, L is the length of the cylinder and Q is the heat flux through the cylinder per unit length. Using equation (6.9) with equation (6.6) it can be shown that the external surface temperature can be reduced to a given temperature in a smaller cylinder wall thickness for a smaller insulation thermal conductivity. Reducing the external wall temperature reduces the radiative power loss, and allows the receiver to obtain a higher temperature.

Radiation insulation employs a radiation shield surrounding the receiver, which reflects the radiative power from the receiver back towards it. This requires that the material has good reflective properties at the Infrared wavelengths (for a blackbody at 2,000 K the peak wavelength of blackbody emission is ~1,500 nm) at the same time as being able to withstand high
temperatures. A selection of materials suitable for the conductive insulation are shown in table (6.3) with their relevant properties:

Table 6.3: Candidate receiver insulation materials [108]

<table>
<thead>
<tr>
<th>Insulation Material</th>
<th>Thermal Conductivity (W·m⁻¹·K⁻¹)</th>
<th>Maximum Operating Temperature (K)</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Graphite Foam</td>
<td>24</td>
<td>3,000</td>
<td>Highest k, highest Max Temp</td>
</tr>
<tr>
<td>MACOR</td>
<td>1.46</td>
<td>1,300</td>
<td>Moderate k, lowest Max Temp</td>
</tr>
<tr>
<td>Zirconia Foam</td>
<td>0.2</td>
<td>2,000</td>
<td>Small k, high Max Temp</td>
</tr>
<tr>
<td>AETB-12</td>
<td>0.06</td>
<td>1,800</td>
<td>Smallest k, Moderate Max Temp</td>
</tr>
</tbody>
</table>

Graphite foam, despite having the highest operational temperature, also has the highest thermal conductivity. Subsequently the insulation package would have to be thicker in order to be effective. MACOR has a reasonable thermal conductivity but its low maximum temperature prevents its use. However it would be appropriate to use MACOR for experimental investigation. Zirconia foam approaches the types of thermal conductivity desired for an actual STP system. AETB-12 is a material consisting of alumina, silica and aluminoborosilicate fibers and is used by NASA for high temperature thermally protective tiles [1, 2004].

A selection of materials suitable for radiative insulation is shown in table (6.4), the spectral reflectance of each material is shown in figure (6.6).
Table 6.4: Candidate materials for radiation shields [108]

<table>
<thead>
<tr>
<th>Insulation Material</th>
<th>Reflectivity @ 1,500nm</th>
<th>Maximum Operating Temperature (K)</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Aluminium</td>
<td>95%</td>
<td>933</td>
<td>High R, lowest Max Temp</td>
</tr>
<tr>
<td>Gold</td>
<td>98%</td>
<td>1,337</td>
<td>High R, High Max Temp</td>
</tr>
<tr>
<td>Silver</td>
<td>99%</td>
<td>1,235</td>
<td>Highest R, Moderate Max Temp</td>
</tr>
<tr>
<td>Nickel</td>
<td>78%</td>
<td>1,726</td>
<td>Lowest R, Highest Max Temp</td>
</tr>
</tbody>
</table>

Aluminium is the cheapest option of the materials identified in table (6.4) and also provides good broad-spectrum reflectivity. However, aluminum has the lowest useable temperature, this means that an aluminum heat shield will need to be placed further away from the hot receiver such that it intercepts a smaller radiative heat flux. If the thermal design of the receiver can maintain the heat shield below aluminium’s maximum operating temperature, then this would be a suitable choice. Gold and silver have excellent reflectivity in the IR and also have much higher maximum operating temperatures. This means that a heat shield made of gold or silver will be more compact than an aluminum heat shield as gold and silver can withstand a higher radiative heat flux. However, their respective costs must be given consideration. Nickel is presented in table (6.4), despite having a relatively low reflectivity it can withstand excessive temperatures and would be appropriate for the higher performance (higher temperature) STP receivers.

6.1.4 Receiver Sizing

The sizing of a direct gain thruster is directly dependant on the input power available and the mass flow rate through the thruster. For a thermal storage thruster we are more concerned that the amount of energy storable within the receiver is sufficient for the maneuver desired. Referring back to section 3, the mission specific requirements allow a first order sizing of each type of thruster. The mass of a thermal storage cavity receiver can be estimated via equations (3.1) and (3.2). The 2 N thermal storage thruster in section 3 had the following requirements:

- Thrust: 2 N
- Minimum Specific Impulse: 400 s
- Burn Duration: 1,200 s
- Heating Time: 3,000 s
Using these parameters in combination with equation (3.1) provides the energy removed per burn from the receiver, this being 4.77 MJ. This is then directly related to the required mass of the Boron carbide (specific heat capacity 2,511 Jkg\(^{-1}\)K\(^{-1}\)) receiver to supply this energy from equation (3.2). For a 1,500 K difference in temperature (assuming a peak receiver temperature of 2,500 K and an end of maneuver temperature of 1,000 K) the required mass of the receiver is estimated at 1.25 kg. The size of the concentrator required to heat the receiver up to a temperature of 2,500 K can now be estimated from equation (3.2). If the mass of the receiver is 1.25 kg and it must be heated up to a temperature of 2,500 K within 3,000 s then according to equation (3.2) the concentrator power requirement is 2,600 W. For an overall system efficiency of 60% then the required concentrator solar collection area is 3.2 m\(^2\).

Estimating the size of a direct gain cavity receiver is a more complex matter, as the heat transfer coefficient \((h_c)\) between receiver and propellant is required for the calculation. Equation (6.10) calculates the heat transfer rate \((\dot{Q})\) to the propellant from the receiver:

\[
\dot{Q} = h_c A \Delta T \quad \text{(6.10)}
\]

Where \(A\) is the transfer area and \(\Delta T\) is the difference in temperature between propellant and receiver. A value of \(\dot{Q}\) and \(\Delta T\) can be estimated from the direct gain thruster requirements of section 3. However the heat transfer coefficient is dependant on the mechanism of heat transfer from receiver to propellant. There are two simple approaches of heat transfer that are most suited to a micro-satellite STP receiver:

- **Single/Multi Pass Channels:** Channels spiral around a central receiver. Flow typically experiences a low-pressure drop and moderate heat transfer efficiency.

- **Particle Beds:** Central receiver, surrounded by a packed bed of heat transfer material particles. Flow typically experiences a high-pressure drop and high heat transfer. However flow instabilities and channeling are a likely consequence of low flow rates and an unsuitable choice of bed material.

Particle bed receivers are more complex than a single/multi channel receiver, because of the need to contain the particle bed within the receiver volume. Thus particle bed receivers tend to be more massive. Also, fine mesh, which is tolerant to high temperatures, is required to keep the particle bed material in place during thrusting, and there is the risk of the bed material clogging up the mesh or throat of the thruster nozzle. Kennedy and Palmer [1, 2004] examined receiver heat transfer options in detail and opted for the simpler solution as more appropriate for rapid testing of STP/FSTP on a micro-satellite. Thus to maintain simplicity a single pass channel heat transfer scheme is selected for the direct gain thruster. Therefore we now consider forced convection flow.
within a circular tube. The quantities describing such heat transfer are the Reynolds number (Re), the Prandtl number (Pr) and the Nusselt number (Nu):

$$\text{Re} = \frac{\dot{m}D}{\mu A} \quad \text{Pr} = \frac{\mu C_p}{k} \quad \text{Nu} = \frac{hD}{k} \quad \ldots \quad (6.11 \text{a,b,c})$$

Here $\dot{m}$ is the mass flow rate, $D$ is the tube diameter, $\mu$ is the propellant viscosity, $A$ is the flow cross-section area and $k$ is the propellant thermal conductivity. In order to calculate the heat transfer coefficient, the correct heat transfer correlation must be selected for the given flow conditions. For a channel flow heat exchanger Petukhov provides a suitable correlation [110, 1987]:

$$\text{Nu} = \frac{f}{8} \left[ \frac{\text{RePr}}{1.07 + 12.7 \sqrt{8 \frac{f}{8} (\text{Pr}^{\frac{2}{3}} - 1)}} \right] \quad \ldots \quad (6.12)$$

Where the Darcy-Weisbach friction factor $f$ is estimated via:

$$f = \frac{1}{(1.82 \log_{10} (\text{Re}) - 1.64)} \quad \ldots \quad (6.13)$$

For ammonia flowing through 1mm bore channels of a hot receiver (2,500 K), the heat transfer coefficient is estimated to be $\sim 2,000 \text{ W/m}^2\text{-K}$. Using equation (6.10) the required heat transfer area can now be estimated to be $\sim 6\text{ cm}^2$. From these sized parameters both types of thruster can now be modelled.

### 6.1.5 Solar Receiver Efficiency

For the solar cavity receiver it is assumed that the receiver acts as a black body and subsequently absorbs all incident radiation. However, there will be a small amount of loss due to light scattered back out of the receiver cavity. Under the assumption that the cavity aperture is as small as possible the receiver absorptivity is likely to be as high as 90%.

### 6.2 Detailed STP Receiver Analysis

In this section the approach taken to model a direct-gain STP receiver is discussed and predicted performances are derived. To model a direct-gain STP receiver, a simulation tool is constructed in Windows EXCEL using visual basic code to automate calculations. A thermal model of the direct-gain receiver is presented based on the fibre optic heat exchanger design introduced in section 6.1.
6.2.1 Direct Gain Receiver Simulation Tool

The direct-gain receiver simulation tool was created, based on a similar tool created by Kennedy [1, 2004] for the purposes of simulating a thermal-storage receiver. The direct-gain tool is constructed specifically for a direct-gain STP system, employing ammonia as a propellant. However, other system characteristics are variable such as, for example, receiver materials and concentrator characteristics. There are two visual basic algorithms which the analysis tool employs. One algorithm computes the receiver temperature rise, during the heating of the receiver prior to thruster operation. The other simulates heat transfer to the propellant, including propellant decomposition, during thrusting and outputs system thrust and specific impulse. This integrated system tool allows the estimation of system behaviour for the UK-DMC demonstrator STP system, providing better insight into the variation of system performance with the number of concentrators employed and other system characteristics.

The receiver heating algorithm makes the assumption that temperature gradients within the receiver are negligible such that a ‘lumped capacity’ model of the receiver is adequate to describe the receiver’s behaviour. However this assumption only holds for small size receivers such as that being considered for a direct-gain STP system. To demonstrate the validity of this assumption the Biot number ($Bi$) of the receiver must be addressed. The Biot number can be considered to be a measure of the relative importance of conduction within a body, compared to the radiative or convective cooling experienced by the body [111, 1994]. Blanchard [111, 1994] defines the Biot number as:
Where \( T_b \) is the body's temperature, \( T_a \) is the ambient temperature, \( L \) is the characteristic length of the body and \( k \) is the body's thermal conductivity. A value of \( Bi \ll 1 \) indicates that the temperature gradients within the body are small and that the 'lumped capacity' approximation can apply. For a direct-gain receiver which is small in size and has a high thermal conductivity suggests that this assumption is adequate. This is further demonstrated in figure (6.8), which plots a body's Biot number vs. the body's temperature (from 300 K to 2,500 K) when the body has a characteristic length of 2 cm and 10 cm, a thermal conductivity of 138 W m\(^{-1}\)K\(^{-1}\) (value for molybdenum) and an emissivity of 1.

![Figure 6.8: Biot number vs. body temperature](image)

From figure (6.8) it is seen that within the temperature range plotted the 'lumped capacity' assumption applies. As the Biot number becomes larger the algorithm will tend to underestimate the receiver temperature providing a more conservative value. The 'lumped capacity' assumption cannot be applied to the insulation package of the receiver as the insulation should experience a large temperature gradient and it is therefore necessary to employ a series of 'lumped capacity' concentric shells to represent the insulation and provide an estimate of the temperature gradients within. With the 'lumped capacity' assumption incorporating Kennedy's [1, 2004] receiver heating algorithm employs an energy balance to calculate how much heat is absorbed by the receiver over a time step \( \Delta t \).

\[
Q_{net} = Q_{in} - (Q_{ns} + Q_{ap} + Q_{cap}) \quad (6.15)[1]
\]
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Where $Q_{net}$ is the energy the receiver absorbs, $Q_{in}$ is the energy delivered by the concentrator system, $Q_{lost}$ is the energy lost through the insulation, $Q_{ap}$ is the energy lost via the receiver aperture and $Q_{cap}$ is the energy lost through the caps at the top and bottom of the cylindrical insulation. The change in receiver body temperature over time step $\Delta t$ is then calculated from the 'lumped capacity' relation:

$$T_{b,i+\Delta t} - T_{b,i} = \frac{Q_{net}}{\rho V_b C_p} \ldots (6.16)[1]$$

It should be noted that the algorithm does not take into account the variation of receiver specific heat capacity ($C_p$) with temperature and it is up to the user to select an appropriate average value. It should also be noted that the formulation of equation (6.16) is explicit and therefore requires appropriately small time step values to remain a stable algorithm.

![Figure 6.9: (a) Depiction of STP thermal-storage receiver. (b) Depiction of STP direct-gain receiver [1]](image)

Kennedy [1] also provides Visual Basic scripts for the simulation of channel flow heat transfer between a thermal-storage receiver and ammonia propellant. These scripts were edited and adapted for the same simulation with a direct-gain receiver. The channel can be seen in figures (6.9a&b) to spiral around the central cavity of the receiver. For the simulation of heat transfer from the receiver to the propellant the channel is separated into a finite number of sections and local Reynolds, Prandtl and Nusselt (equations 6.11 – 6.13) numbers are estimated in order to find the channel section heat transfer coefficient.

It is necessary to keep track of the pressure change ($\Delta P$) between the propellant supply pressure and the pressure at the exit plenum to get an accurate estimate of the thruster specific impulse. The algorithm accomplishes this via:
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\[
\frac{\Delta P}{L} = f \frac{P_{u}^{2}}{2D} \quad (6.17) [110]
\]

Where \( L \) is the channel length, \( \rho \) is the flow density \( u \) is the local flow velocity, \( D \) is the hydraulic diameter and \( f \) is the Darcy-Weisbach friction factor given by equation (6.13).

It is important to note that Kennedy’s algorithm [1, 2004] takes into account the decomposition of the ammonia propellant to get a more accurate estimate of the performance of the STP thruster. Ammonia decomposition in the algorithm is modelled as an equilibrium process, without any intermediate components, such that at a given location along the channel equilibrium constant (\( K_p \)) can be defined:

\[
K_p(Q^r) = \frac{p_{p_m}^{\alpha_m} p_{p_n}^{\alpha_n}}{p_{p_m}^{\beta_m} p_{p_n}^{\beta_n}} \quad (6.18) [1]
\]

Where \( K_p \) is dependent on the partial pressures of the reactions products \( (p_{p_m}, p_{p_n}) \) and reactants \( (p_{p_m}, p_{p_n}) \) raised to the stoichiometric exponents \( \alpha, \beta, \mu, \) and \( \nu \):

\[
\alpha A + \beta B \leftrightarrow \mu M + \nu N \quad (6.19) [1]
\]

These are tabulated for a variety of reactions across a wide range of temperatures, and include ammonia and methane decomposition. Then, applying the well-known Gibbs-Dalton Law:

\[
K = \frac{P}{RT} \quad (6.20) [1]
\]

Allows the determination of the relative concentration (mole fraction) for each constituent, for a known temperature and pressure as \( K_p \) is an equilibrium constant formed from constituent mole fractions \( (\chi_m, \chi_n, \chi_A, \chi_B) \):

\[
K_p(Q^r) = \frac{\chi_m^{\alpha_m} \chi_n^{\alpha_n}}{\chi_A^{\beta_m} \chi_B^{\beta_n}} \quad (6.21) [1]
\]

With the propellant mole fractions identified for a particular channel location more accurate estimates can be made of the local conditions and gas properties.

### 6.2.2 Simulated Performance of UK-DMC Demonstrator STP System

The direct-gain STP integrated analysis tool was employed to determine suitable receiver dimensions, model receiver heat up characteristics and simulate a receiver burn for the UK-DMC demonstrator direct-gain STP system. The characteristic dimensions required of the thruster is depicted in figure (6.10) and stated in table (6.5). The characteristic dimensions for the receiver are the inner diameter (ID) outer diameter (OD) and receiver length (L).
Table 6.5: UK_DMC demonstrator direct-gain receiver properties and requirements

<table>
<thead>
<tr>
<th>Receiver Type</th>
<th>Direct-gain</th>
</tr>
</thead>
<tbody>
<tr>
<td>Receiver Material</td>
<td>Molybdenum</td>
</tr>
<tr>
<td>Receiver Mass (g)</td>
<td>20</td>
</tr>
<tr>
<td>ID (mm)</td>
<td>Variable dependent on the number of concentrators</td>
</tr>
<tr>
<td>OD (mm)</td>
<td>15</td>
</tr>
<tr>
<td>L (mm)</td>
<td>20</td>
</tr>
<tr>
<td>Insulation Material</td>
<td>AEBT-12</td>
</tr>
<tr>
<td>Insulation Thickness (mm)</td>
<td>30</td>
</tr>
<tr>
<td>Required Thrust (N)</td>
<td>0.02</td>
</tr>
<tr>
<td>Required Specific Impulse (s)</td>
<td>300 – 350</td>
</tr>
<tr>
<td>Required Burn Duration (s)</td>
<td>300</td>
</tr>
<tr>
<td>Propellant</td>
<td>Ammonia</td>
</tr>
<tr>
<td>Nozzle Area Ratio</td>
<td>400</td>
</tr>
</tbody>
</table>

In table (6.5) the value for the ID for each case has been scaled to a minimum size dependent on the accumulative area of the spot for each concentrator in the system. The losses due to optical path length are considered best case and are taken from the values stated in chapter 4 and in the literature; these values are reiterated in table (6.6):
Table 6.6: Direct-gain UK-DMC demonstrator system best case losses.

<table>
<thead>
<tr>
<th>Efficiency</th>
<th>Value (%)</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Primary Transmission</td>
<td>95</td>
<td>Consistent with Feuermann and Kennedy [52, 1999][1, 2004]</td>
</tr>
<tr>
<td>Secondary Transmission</td>
<td>95</td>
<td>Consistent with Feuermann and Kennedy [52, 1999][1, 2004]</td>
</tr>
<tr>
<td>Transmission at fibre tip surface</td>
<td>96</td>
<td>Consistent with Feuermann and Kennedy [52, 1999][1, 2004]</td>
</tr>
<tr>
<td>due to Fresnel reflection.</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Dynamic Tracking Efficiency</td>
<td>100 – 95</td>
<td>Required in order to provide minimum acceptable efficiency.</td>
</tr>
<tr>
<td>(5% tolerance)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fibre Optic Transmission</td>
<td>85</td>
<td>Observed fibre optic transmission for FLU 7 m fibre from Feuermann and Gordon [52, 1999][55, 2002][51, 2003]</td>
</tr>
<tr>
<td>Transmission at fibre tip surface</td>
<td>96</td>
<td>Consistent with Feuermann and Kennedy [52, 1999][1, 2004]</td>
</tr>
<tr>
<td>due to Fresnel reflection.</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Receiver Absorption</td>
<td>100-90</td>
<td>Kennedy [1, 2004]</td>
</tr>
<tr>
<td>(10% tolerance)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total Efficiency</td>
<td>70-60</td>
<td></td>
</tr>
</tbody>
</table>

It should be noted that the total efficiency range stated in table (6.6) is consistent with that reported in the literature [101,112, 1, 50, 52]. With the values stated in tables (6.5 & 6.6) the STP demonstrator direct-gain receiver is simulated in heat up and during a 300 s maneuver, a total system efficiency of 60% is assumed. After discussion with the propulsion engineers of SSTL it was decided that the STP demonstrator system should be capable of at least 300 s specific impulse in order demonstrate the benefit of the system in comparison to mono-propellant hydrazine (capable of 220 s specific impulse) [113, 2005].
Figure 6.11: Temperature vs. time plot for UK-DMC demonstrator receiver heat-up with 6 mirror concentrator system.

Figure (6.11) shows the transient heating of the receiver. At the peak receiver temperature (1,620 K) full decomposition of the ammonia propellant is possible; and desirable, as the exhaust products will consist mostly of hydrogen and nitrogen which have less molecular weight than the original ammonia, therefore increasing achievable specific impulse.

Figure 6.12: Propellant temperature vs. channel length for UK-DMC demonstrator receiver with 6 mirror concentrator system.

In figure (6.12) the propellant temperature is plotted against channel position. The channel length is scaled suitably such that the propellant achieves the current peak receiver temperature just upon exiting the receiver. In doing so the propellant resident time within the receiver is minimised and the efficiency of the heat transfer between the propellant and the receiver is maximised.
Figure 6.13: Propellant temperature & receiver temperature vs. time for UK-DMC demonstrator receiver with 6 mirror concentrator system.

Figure (6.13) plots the variation in propellant and receiver temperature over the duration of the 300 s burn. As the propellant begins to flow through the receiver the receiver cools and over the course of the short burn, approaches lower steady state temperature. As the propellant temperature decreases the system performance will also decrease due to the lower propellant temperature and a decrease in the level of propellant decomposition.

Figure 6.14: Peak receiver temperature vs. number of concentrators

In figure (6.14) the variation of peak receiver temperature with number of concentrators is shown. The peak receiver temperature is the temperature at the end of the receiver heating, prior to the thruster operation. The length of time required to obtain these temperatures is inversely proportional to the number of concentrators used, taking up to 30 minutes for a single concentrator to heat the receiver to a peak temperature of ~1,000 K. Once the receiver has obtained peak temperature, the thruster burn simulation is started and two parameters are noted,
these being the burn average specific impulse after 300 s and the steady state specific impulse value. These parameters are plotted in figure (6.15).

![Graph showing specific impulse vs. number of concentrators](image)

**Figure 6.15: 300s Burn Average I<sub>sp</sub> and Steady State Burn I<sub>sp</sub> vs. number of concentrators**

In figure (6.15) the 300 s duration burn average specific impulse is large in comparison to the steady state value for small numbers of concentrators. This can be explained by the relatively high temperature of the receiver at the start of the burn. As the two curves approach each other there is an inflection point in which the gradients of the curve become much more similar, this is due to the thruster experiencing full decomposition of the propellant. From these simulation results it is clear that in order for the system to be able to demonstrate a minimum of 300 s specific impulse performance a 6 or 7 concentrator system is required. However, these results facilitate to predict expected performance for a system with any number of concentrators.

### 6.3 Fibre Optic Heat Exchanger Investigation

For the purposes of investigating fibre optic heating two experiments were conducted. The first experiment conducted investigated the fibre optic heating of a small graphite sample. The second was the fibre optic heating of a heat exchanger, based on the thermal design of a direct-gain receiver as discussed earlier in this chapter.

#### 6.3.1 Graphite Sample Heating Tests

In the winter of 2004 initial tests were conducted at SSTL’s solar simulator facility to determine the high intensity light transmission efficiency of the FLU fibre optic cable and determine the feasibility of harnessing the transmitted power to heat a small graphite sample. The experimental set up is shown in figure (6.16).
The fibre optic cable is coupled to a 14 cm diameter parabolic concentrator (only available concentrator at the time of the experiment, this concentrator was designed and manufactured by Kennedy [1, 2004]) on an optical rail aligned with the aperture of a xenon arc lamp solar simulator. The solar simulator itself produces AM0 intensity and higher over a 12cm diameter beam. A collimating lens is used to reduce divergence of the solar simulator beam. The other end of the fibre optic cable is inserted into a small graphite sample wrapped in insulation, in order to reduce convective effects. The temperature of the graphite sample is monitored by a thermocouple. The beam from the solar simulator is concentrated by the parabolic mirror into the FLU fibre optic cable. This energy traverses the fibre optic and is delivered to heat the graphite sample. To determine the transmission efficiency the graphite sample was also heated directly by the parabolic mirror. The temperature time graphs for both direct heating and fibre heating are shown in figure (6.17), with corresponding polynomial fits to aid visualisation:
The power used to heat the graphite sample is estimated from the initial gradient of the best-fit lines shown in figure (6.17). The effective heating power estimated for direct heating by the parabolic concentrator was 10.5 W taking into account the area of the mirror obstructed by the graphite sample and insulation. In the same way the effective heating power estimated for fibre heating was 4.9 W, providing an overall fibre optic heating efficiency of 47%. Assuming a concentrator and fibre efficiency of 50%, which is consistent with the transmission tests discussed in chapter 5 section 5.3.1, then the absorption efficiency of the graphite sample is 93%. Here it first was observed that the spot size produced by the concentrator was significantly larger than the fibre tip area. This was believed to be due to divergence of the solar simulator beam and results in a reduced intensity at the graphite sample. Kennedy [1, 2004] evaluated the size of the spot for this concentrator using the same optical rail and solar simulator set up as shown in figure (6.16). Kennedy estimates the size of the concentrator spot to be 3 mm in diameter and Kennedy also notes that the spot has a bright centre 1mm wide. As discussed in chapter 4 the intensity distribution of the concentrator spot is most intense at its centre (demonstrated by simulation and experiment), explaining how the 1 mm diameter fibre was able to capture the 4.9 W of power. However, a large portion of the power delivered by the concentrator will not enter the fibre, indicating the potential for greater transmission efficiencies with better light collimation. These tests appeared to corroborate with the claims in the literature of transmission efficiencies of greater than 50%, taking into account the limitations of the experiment, and served to demonstrate effective heating via fibre optic transmission of high intensity sunlight.

6.3.2 Fibre Optic Heat Exchanger Investigation

The heating tests conducted in December 2004 (see section 6.3.1), although encouraging, did not provide any useful information regarding the thermal design of a direct-gain solar receiver for the UK-DMC STP demonstrator system, with especial emphasis on maintaining the FLU fibre optic cable temperature below the manufactures specified maximum operation temperature. Subsequently a fibre optic heat exchanger, representative of a direct-gain solar receiver, was designed, manufactured and tested based on the fibre-receiver coupling concept discussed in chapter 5 (see figure (5.8b)). This section describes the efforts undertaken and lessons learned in the investigation of this fibre optic heat exchanger concept.

6.3.2.1 Fibre Optic Heat Exchanger Thermal Design

In the literature the only reported demonstration of a fibre optic heat exchanger for the purposes of solar thermal propulsion was by Nakamura [10], who harnessed 240W of fibre optic transmitted solar energy, via 4, 0.5 meter diameter parabolic reflectors, to heat a graphite cylinder and nitrogen working fluid up to a temperature of 1,700K (see figure (6.18)).
The experiments conducted by Nakamura successfully demonstrate the effective heating of a propellant via a fibre optic heat exchanger. However, there appeared to be a number of complications and avoidable inefficiencies in the design of this heat exchanger. In particular the fibre optic cables were required to be water cooled during heating and the heat exchanger design did not make use of a black body cavity, in that the fibre optics heated the outside of a hollow cylinder rather than the inside. This not only caused the fibre optic heat exchanger to be unnecessarily inefficient but also bulky and over complicated. In this way the Nakamura fibre optic heat exchanger was not considered to be applicable to a fibre optic heat exchanger for the UK-DMC STP demonstration system. The fibre optic heat exchanger design concept, discussed in chapter 5 (see figure (5.8b)), considered to be conducive to the UK-DMC STP demonstrator system was subsequently developed. The design of this fibre optic heat exchanger is shown in figure (6.19).
The heat exchanger design shown in figure (6.19) consists of a MACOR insulation package surrounding a molybdenum cavity receiver. The molybdenum receiver is representative of the sized direct-gain solar thermal receiver for the UK-DMC demonstration system, in terms of mass and dimensions. The insulation package is surrounded by a heat shield to further reduce radiative energy losses to the environment. The fibre is inserted into the receiver, but is held rigidly so that it does not come in to contact with the receiver cavity wall. This arrangement allows only indirect conductive contact between the fibre and receiver, via the heat exchanger. Allowing the fibre optic to be only in direct contact with the receiver in a radiative sense should result in the fibre optic being protected from the high receiver temperatures.

Simulations of insulation performance and cavity receiver-to-fibre interaction were conducted to characterize the theoretical thermal behaviour of the heat exchanger at its maximum temperature limit of 1,200 K. This maximum temperature limit is set for maximum useable temperature of the MACOR insulation, this being 1,273 K. With this imposed receiver temperature limit, the vacuum steady-state performance of the insulation package and heat shield combination was simulated using a cylindrical heat transfer model of the combined structure.

Figure (6.20) shows the steady state temperature variation of the heat exchanger with radius. The initial temperature drop is a result of combined conductive and radiative interaction between the receiver and the inner wall of the MACOR insulation. The temperature decreases further to the outside of the MACOR insulation, which is 10 mm thick\(^{28}\). A very steep drop in temperature then occurs between the insulation and the heat shield, which are assumed to only interact radiatively. This analysis is insightful as to which materials are appropriate for the structure of the heat exchanger. Stainless steel supports are deemed suitable to secure the insulation package, however, additional insulation will be required between the two to minimise further heat loss. Due to the

\(^{28}\) Dimensions of vacuum bell jar restricted insulation thickness to this value.
relatively low temperature reached by the heat shield, aluminium was deemed suitable. Its low cost and availability also make it preferable.

A bundle of seven fibres is chosen for heating the receiver, with each fibre connected to a small concentrator. In this case, according to figure (6.14), a peak receiver temperature of 1,600 K would be achievable. A bundle of seven fibres will also require a receiver aperture of 5mm in diameter. The radiative interaction between the fibre optic bundle and the cavity receiver was simulated using a cylindrical view factor model of the cavity as shown in figure (6.21). Here the fibre bundle tip is placed just at the entrance to the cavity receiver. A transient finite difference technique was employed to model the temperature variation along the length of the fibre and most importantly the steady state temperature of the fibre tip taking into account the absorption spectrum of the fibre optic.

In figure (6.21), the view factor model indicates that the steady-state temperature at the hot end of the fibre bundle (352 K) is much less than that at the cavity receiver, even with its proximity to the cavity wall (R = 5 mm and r = 3 mm). It is also worth noting that the fibre bundle takes approximately 6 hours to reach steady state temperature, which is much longer than that required to heat-up the receiver. Pure, fused silica fibre optics can be safely employed at such temperatures without degradation in optical properties. It should be noted, however, that this model assumes that the receiver support structure remains at 300 K for the simulation duration.

6.3.2.2 Heat Exchanger Testing

The fibre optic heat exchanger thermal design was sent to the University of Surrey engineering workshop for manufacture. In order to test the heat exchanger in vacuum, the heat exchanger was required to be small enough to fit inside a vacuum sealed bell-jar (see figure (6.22)).
Once the fibre optic heat exchanger had been manufactured, a fibre optic bundle consisting of seven fibres was manufactured. Although seven small concentrators were not actually available, it was hoped that sufficient Solar power could be harnessed from a 0.5 m parabolic concentrator, available from a previous research project. However, with a preliminary heat exchanger test, it was found that the available Losmandy solar tracking tripod mount was not capable of accurately tracking the Sun when supporting the weight of the concentrator (see figure (6.23)).

In order to salvage the situation, it was decided to attempt heating tests with the available small concentrators. This would result in a significant decrease in available power, but could serve to demonstrate effective heating regardless. A series of single small concentrator heating tests were subsequently conducted at the SSTL solar simulator facility. This allowed for a high intensity solar source and accurate alignment of a fibre tip with the concentrator focal point. The fibre optic was fed through a hole in the bell jar base plate, which was then sealed with adhesive sealant. With the fibre in place a thermopile detector was used to confirm the fibre power output to be 4.75 W before the bell jar was placed over the heat exchanger. A C type thermocouple was also
fed through a hole in the bell jar base plate and sealed in. Finally a vacuum pump was used to reduce the air pressure within the bell jar containing the fibre optic heat exchanger to $-1 \times 10^1$ torr, reducing convective cooling. This was the best vacuum that could be obtained with the available equipment. During the heating tests, the receiver temperature was measured by the thermocouple at regular intervals of approximately 20 s.

The first of the heating tests yielded very poor heating, with the heat exchanger only achieving a temperature of 375 K for a 600 s heating time. It was thought that this was most likely due to poor absorption of the fibre transmitted energy, by the bare internal surface of the molybdenum receiver. Consulting the literature, in an attempt to increase the absorption efficiency of the molybdenum receiver, provided two possibilities. The first of which [37, 2000] was the use of high temperature resistant black paint to coat the interior of the receiver. Further heating tests with the receiver’s interior coated in this black paint, yielded better results. The results demonstrated that the molybdenum receiver attained a temperature of 470 K for a 1,200 s heating time. The other method [112, 2006] for increasing receiver absorption efficiency was to coat the interior of the receiver with graphite. When the receiver interior was coated with graphite, heating tests yielded a higher peak temperature of 512 K. A further heating test was conducted, in which power was supplied to the molybdenum receiver from two concentrators, the solar simulator illuminated one and the other was illuminated by the actual Sun (see figure (6.24)).
Unfortunately only enough fibre was available for one Sun illuminated concentrator (requiring 10 m of fibre per concentrator). However, the additional power supplied by the other concentrator (~2 W) was able to further increase the final temperature of the heat exchanger to 555 K. Throughout these tests the same fibre optic cable was used and no increase in attenuation was observed when the fibre was exposed to the relatively high temperature receiver. Measurement of the fibre optic temperature during receiver heating did not show any significant rise in temperature. The results of these tests are shown in figure (6.25). The radiative absorption efficiency exhibited by the fibre optic heat exchanger is estimated by calculating the heating power experienced by the heat exchanger, based upon the initial gradient of the temperature time heating data in figure (6.25), and comparing it to the fibre optic output power. This is the same calculation as performed for the graphite sample heating tests in section 6.3.1. Table (6.7) lists the estimated heat exchanger absorption efficiencies for the varying cases:

<table>
<thead>
<tr>
<th>Heat exchanger cavity interior coating</th>
<th>Fibre optic transmitted power (W)</th>
<th>Calculated heating power (W)</th>
<th>Absorption efficiency (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bare receiver</td>
<td>4.75</td>
<td>2.55</td>
<td>54</td>
</tr>
<tr>
<td>High temperature black paint</td>
<td>4.75</td>
<td>3.50</td>
<td>74</td>
</tr>
<tr>
<td>Graphite</td>
<td>4.75</td>
<td>3.83</td>
<td>81</td>
</tr>
<tr>
<td>Graphite 2 concentrators</td>
<td>6.75</td>
<td>5.51</td>
<td>82</td>
</tr>
</tbody>
</table>

Table (6.7): Heat exchanger absorption efficiency for varying cavity interior coatings.

Figure 6.25: Temperature time graphs for fibre optic heat exchanger heating tests.
The measurement error for the data presented in figure (6.25) is estimated to be approximately ± 9K, and is due to fluctuations in the thermocouple temperature readout and the time at which the measurement was manually taken. The manufacturer standard limit of error for the C type thermocouple is ± 4.5 K. The “graphite 2 concentrator” curve was compared to a model of the receiver created with the direct gain receiver simulation tool. A receiver heat-up simulation was constructed, taking into account available power and the large size of the receiver aperture. The simulation was run twice, once with the receiver absorbing 100% of the available power and once with the receiver absorbing 50% of the available power. For both simulations a perfect vacuum was assumed. The simulation temperature time plots are shown in figure (6.26).

From figure (6.26) it can be seen that the shape of the “graphite 2 concentrator” data is initially more similar to the “100%” plot which is expected, given the calculated absorption efficiency of the heat exchanger to be ~82%. However, the “50%” plot approximately agrees with the heating data for the peak receiver temperature. This is also seen when comparing the initial temperature gradient of the real data to the two simulation plots. This suggests that the receiver is efficiently absorbing the power delivered by the concentrator, via the fibre optics and explains the initial increase in heat exchanger temperature, with time being more consistent with the 100% model. However, the thermal design of the fibre optic heat exchanger is not optimal, and suggests the presence of inefficiencies such as convective cooling, occurring due to the imperfect vacuum in the bell jar, which results in the peak heat exchanger temperature agreeing more with the 50% model at steady state.
It is clear that the fibre optic heat exchanger for the UK-DMC STP demonstration system requires further thermal development. Future tests to improve efficiency, should utilise better vacuum conditions and better insulation materials. However, this investigation was able to provide "know how" into improving absorption efficiency and experimentally demonstrate the heat exchanger to be capable of maintaining the FLU fibre optic temperature below its maximum useable limit for receiver temperatures up to 555 K (122 K above the maximum useable limit).

6.4 Chapter Summary and Conclusions

6.4.1 Chapter Summary

In this chapter the investigation of developing a direct-gain solar receiver for a fibre augmented solar thermal propulsion demonstration system has been described. Initially, the theory of propulsion and heat transfer was discussed and the materials appropriate to STP receivers were reviewed. A design tool created specifically for the analysis of ammonia direct-gain STP thrusters is discussed and employed to size, design and assess the performance of a direct-gain STP receiver being heated from several small fibre optic coupled concentrators. Finally, a series of practical tests were performed to investigate fibre optic heating of small graphite samples in air and a molybdenum receiver in vacuum.

6.4.2 Chapter Conclusions

From the analysis and discussion in this chapter the following conclusions were made:

- For a direct-gain receiver refractory metal materials offer the most appropriate material properties. Molybdenum was selected for the UK-DMC demonstrator STP system due to its large thermal conductivity in comparison to other candidate materials.

- For a thermal-storage receiver, high heat capacity ceramics offer the most appropriate material properties. Boron Carbide was selected for the HLEO mini-satellite constellation STP system due to its large specific heat capacity in comparison to other candidate materials.

- Conductive insulation materials for a STP receiver require very low thermal conductivity. (AETB-12 thermal conductivity of 0.06 W·m⁻¹·K⁻¹) This will ensure a compact receiver design, which subsequently reduces radiative heat loss and increases achievable peak receiver temperature.
• Sizing of the 2-N thermal-storage receiver for the HELO mini-satellite STP system resulted in a boron carbide receiver mass of 1.25 kg, which requires a 1.5 m diameter concentrator for heating.

• Sizing of the UK-DMC STP demonstrator direct-gain receiver resulted in a molybdenum receiver mass of ~20 g.

• For an overall UK-DMC STP demonstrator system efficiency of 60% at least 6 105 mm diameter parabolic concentrator mirrors are required to achieve the minimum required specific impulse of 300 s.

• Graphite sample heating tests demonstrated an overall heating efficiency via fibre of 47%. This efficiency was due to the low concentration achievable with the available parabolic concentrator mirror. With greater concentration this heating efficiency can be improved. The absorption efficiency of the graphite sample was estimated at 93%, which demonstrates the feasibility of achieving the high required receiver absorption efficiency of 95%.

• Thermal modeling of fibre optic coupled to a direct-gain receiver indicated the feasibility of the fibre thermal protection concept of placing a fibre optic only in radiative contact with the receiver. Resulting in the fibres temperature remaining below the manufacturers limit at steady state (fused silica FLU fibre optic manufacturers maximum useable temperature 460 K, model indicates a steady state temperature of ~350 K)

• Coating the interior of the fibre optic heat exchanger molybdenum receiver significantly improves receiver absorption efficiency from 50% to 82%.

• Heating tests of the fibre optic heat exchanger demonstrates potential for high receiver temperature with increased heating power. However, experimental errors and inefficiencies limited peak receiver temperature in practice.
Chapter 7

7 Solar Concentrator Pointing Control
System Development

This chapter describes the development of a novel method for solar concentrator pointing control via a unique sensor, capable of directly measuring the angular displacement between the concentrator solar image and the fibre optic cable tip. To begin with the need for such ability is discussed, the system requirements are identified and the novel sensor concept is introduced. Then sensor measurement algorithms are discussed and finally the efforts to develop the pointing system hardware are presented.

7.1 Concentrator Pointing Control

In this section the need for a pointing control system for a small spacecraft STP system is presented and the pointing requirements of such a system are identified. The advantages of augmenting the pointing control system with fibre optics are discussed and a novel concept for a concentrator pointing sensor is introduced. Finally essentials in control theory and space mechanism actuation are discussed throughout.

7.1.1 STP Concentrator Pointing Control Requirements

Pointing the concentrator system to align accurately with the sun-vector is critical to the success of the system. The pointing accuracy requirement is directly related to the receiver aperture/fiber optic (or bundle) diameter and the concentrator image size. It was shown in chapter 6 that the optimal size of the receiver aperture was the size of the concentrator spot, as this results in a higher maximum receiver temperature. In chapter 4 it was shown how the size and shape of a parabolic concentrator mirror could be adjusted such that it produced an image consistent with the size and numerical aperture of a given fibre optic. Therefore by assuming that the concentrator image is the same size as the fibre optic, which is the same size as the receiver aperture, an analysis of pointing error vs. power loss can be conducted.
In figure (7.1) the effect of angular pointing error ($\theta$) on the power coupled to the fibre optic from the concentrator is depicted. It is clear that $x = F\tan\theta$ hence that angular errors will result in larger displacements ($x$) between the solar image and the fibre optic resulting in the fibre optic intercepting less of the incident radiative power from the solar concentrator. To accurately attain the relationship between this power loss and pointing error, it is necessary to consider the concentrator image (spot) intensity distribution, identified in section 4.3.2 (see figure (4.11)). This was done for the concentrator design of chapter 4, which is therefore coupled to a 1mm fibre optic cable with a numerical aperture of 0.66 (such that $F = 72$mm). The coupling efficiency (percent of radiative power intercepted by the fibre in comparison to the total power within the spot) between the concentrator and fibre optic was calculated for a range of angular pointing errors, see table (7.1), by integrating the intensity distribution of the Spot over the fibre optic tip. This process is described in section 7.2.

<table>
<thead>
<tr>
<th>Angular Pointing Error ($\theta$)</th>
<th>Displacement ($x$) (mm)</th>
<th>Coupling Efficiency (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5°</td>
<td>0.63</td>
<td>32</td>
</tr>
<tr>
<td>0.2°</td>
<td>0.13</td>
<td>80.6</td>
</tr>
<tr>
<td>Arcmin (1°/60)</td>
<td>0.021</td>
<td>98</td>
</tr>
<tr>
<td>30 Arcsecond (30°/3600)</td>
<td>0.01</td>
<td>99.5</td>
</tr>
<tr>
<td>Arcsecond (1°/3600)</td>
<td>0.00035</td>
<td>99.97</td>
</tr>
</tbody>
</table>
It should be noted that although the values shown in table (7.1) are for only one size of concentrator, which results in specific $x$ values, the power loss is the same at a given angular pointing error for any size of concentrator with the same rim angle value.

In figure (7.2a) the relationship between pointing error and coupling efficiency is shown graphically. In figure (7.2b) the subsequent relationship between pointing error and specific impulse is plotted for the six-concentrator, direct-gain, STP demonstrator system for the UK-DMC. The values plotted in figure (7.2b) were calculated using the ammonia direct-gain receiver simulation tool described in section 6.2.1.

In chapter 6, section 6.2.2, table (6.6) lists a series of system component efficiencies, which resulted in an overall system efficiency of 60%. It was found through simulation of the demonstrator STP ammonia direct-gain receiver that for a system efficiency of 60% the minimum required specific impulse of 300 s could be achieved, for a concentrator system consisting of six concentrators. Included in the list of component efficiencies in table (6.6) is the tolerable efficiency for tracking the Sun in a dynamic environment (95% tolerable coupling efficiency). Therefore, to achieve this tolerable coupling efficiency, a concentrator pointing system must align the solar image with the fibre optic tip to within 2 arc-minutes. Furthermore, the concentrator pointing system must be able to do this whilst being located in the dynamic environment of a small spacecraft.
A concentrator pointing system must be able to accurately track the Sun's motion across the sky, which is dependant upon the satellite's orbit and attitude motion. Considering the demonstrator STP system for the UK-DMC spacecraft, the UK-DMC spacecraft is a NADIR orientated spacecraft, such that one face of the spacecraft is always facing the Earth. This facet supports the Earth observing payload. The STP system would likely be located on the space-facing facet of the spacecraft. In the body frame of the UK-DMC spacecraft the Sun vector from the origin of the body frame can be expressed via two angles, the SunElevation angle and the SunAzimuth angle as shown in figure (7.3).

\[ \text{SunElevation range: } -10^\circ \text{ to } 62^\circ \text{ (72° range)} \]

\[ \text{SunAzimuth range: } 28^\circ \text{ to } 152^\circ \text{ (124° range)} \]

In figure (7.3) it should be noted that the z-axis of the spacecraft body frame of reference is always pointing towards the centre of the Earth and x-axis is always pointing in the direction of the spacecraft's motion. In figure (7.4) the variation of the Sun elevation and azimuth angles are shown for a two hour period around the spacecraft's orbit. The data shown in figure (7.4) was obtained from an STK model of the UK-DMC spacecraft, which is shown in figure (7.3). Figure (7.4) indicates that if the demonstrator STP system for the UK-DMC were to operate for the entire Sun-lit portion of an orbit, the range of movement required of the concentrator pointing system would be:

- SunElevation range: -10° to 62° (72° range)
- SunAzimuth range: 28° to 152° (124° range)
Furthermore, figure (7.4) also provides the maximum rate at which the Sun elevation and azimuth angles change, providing the angular rate at which the concentrator pointing control system must track:

- SunElevation angle maximum rate: 0.054°/s (194arc-seconds/s)
- SunAzimuth angle maximum rate: 0.1135°/s (409arc-seconds/s)

If the concentrator pointing system were an open-loop system then its pointing accuracy is only as good as the satellite platforms attitude control accuracy. In the case of the UK-DMC the attitude control accuracy is to within ±0.5° [114, 2005], which is a typical value for small-satellites ([115, 2005] ranges between ±0.1° and ±0.5°). From figure (7.2b), it is clear that an open-loop pointing system for the UK-DMC would severely reduce the performance of the STP system and therefore stresses the need for a closed-loop concentrator pointing control system, to improve STP system performance and robustness.

Spacecraft are also known to experience structural vibrations significant enough to affect the performance of imaging and optical payloads. An example of this is the structural monotone vibration measured by Bamber and Palmer [116, 2006] on several spacecraft of the DMC constellation. Bamber was able to measure this vibration via analysis of images taken with the DMC spacecraft imaging system. This structural vibration has a frequency of ~0.6 Hz with a magnitude as large as ~75 arc-seconds. Bamber and Palmer also note observing, on several occasions, vibrational shocks exceeding this (up to 300 arc-seconds)[117, 2006]. This structural vibration was thought to be due to the functioning of the momentum wheels onboard each of the spacecraft. Spacecraft vibrations generally arise from systems with moving parts or flexible structures, such as [118, 1998]:

- Momentum wheels
- Cryo-coolers
- Deployable solar panels
- Deployable antennas
- Deployable booms

If the pointing system were unable to compensate for such disturbances a significant reduction in power transmission could be observed. Toyoshima and Araki [119, 2001] measure the angular micro-vibrations of platform jitter on a three-axis attitude-stabilized geo-stationary test satellite in orbit. The satellite, referred to in the literature as Test Satellite VI, weighs approximately 2,000 kg and possesses 30m solar panels. Toyoshima measures the angular vibrations using an onboard laser communication payload, which provided a tracking accuracy of 0.2 arc-seconds and a
sampling rate of 500 Hz. Spacecraft jitter was measured in orbit with the largest vibrational amplitudes measured to be ~10 arc-seconds at a frequency of 5 Hz. Toyoshima notes that the measured vibrations of the Test Satellite VI diminish in amplitude with increasing frequency.

Woodard et al [120, 1998] report on the effect of spacecraft micro-vibrations on atmospheric science payloads onboard the Upper Atmosphere Research Satellite (UARS), launched in 1991. UARS is a large spacecraft (35ft long) weighing around 5,000 kg. Woodard reports observing structural vibrations, which are attributed to the solar arrays of the UARS spacecraft, with amplitudes as high as 20 arc-seconds at a frequency of 0.25 and 1 Hz. Furthermore, Woodard observes disturbance events occurring every orbit with angular disturbances as high as 60 arc-seconds. These events are attributed to thermal shock in the spacecraft and the motion of the solar panels.

The demonstrator STP system for the UK-DMC spacecraft must be able to track (to within arc-second accuracies) the Sun vector over the range of movement specified earlier and attenuate vibrational disturbances large enough to cause significant power losses, such as the vibrational disturbance measured by Bamber and Palmer for the UK-DMC.

7.1.2 Spacecraft Pointing Mechanism Actuators

Pointing mechanisms are common on spacecraft for the fine pointing of antennas and scientific instruments. Often, the required range of movement and scale of the mechanism dictates the type of movement actuation employed. Suhonen et al [121, 2001] discuss the development of a micro-mechanical mirror for fine pointing and stabilization of laser beams for inter-satellite communication systems. The mechanism employs electrostatic forces as a means of actuation. The mechanism has an angular range of ±0.2° with an angular resolution of 0.07 arc-seconds and the mirror is 23mm in diameter. The control bandwidth of the mechanism is 40 Hz and the average power consumption is 1W. Astrium [122, 2005] manufacture antenna pointing mechanism for geo-stationary satellites, one such mechanism is shown in figure (7.5b). This mechanism employs two stepper motors for actuation. The mechanism has a range of ±10° with an angular resolution of 0.005°. The mechanism can support a load of up to 24kg and has a pointing velocity of 5°/minute.

In the previous section two degrees of movement (elevation and azimuth) were required for tracking the Sun vector in the body reference frame of the UK-DMC. Therefore a mechanism suitable for orientating a concentrator with the Sun vector must have the ability to rotate in the elevation and azimuth directions over the ranges specified. Clearly electric motors are a suitable choice for actuation of such a mechanism.
There are four types of motors in common use for space mechanisms [123, 1995][118, 1998]:

- Brush Direct Current (DC) Torque Motors
- Limited-Rotation, Brush-less DC Motors
- Continuous-Rotation, Brush-less DC Motors
- Stepper Motors

The first three of these are regarded as torque motors [123, 1995][118, 1998]. Torque motors directly control an applied torque such that the torque produced by the motor is directly proportional to the voltage applied to the motor. They are typically used in a closed-loop servo system to control position, velocity or acceleration and they are known to be nearly 100% power efficient. Brush torque motor life is severely limited in the space environment as the lubricating effect of an atmosphere is unavailable to maintain the brushes. However, the brush-less torque motor life is limited only by any bearing or gears used. Torque motors are typically capable of rotary speeds of up to 10,000 rpm, suggesting their suitability for high bandwidth applications [123, 1995][118, 1998]. It should be noted that the limited-rotation, brush-less DC motor is inherently unsuitable for the concentrator pointing mechanism as the motor moves only through narrow angles.

Stepper motors directly control position and can be used in an open or closed loop servo system to control velocity or acceleration. A stepper motor is normally driven with square-wave voltage pulses, each of which lasts a few milliseconds, which are switched sequentially from winding to winding, causing the motor to jump step-wise. Their power efficiency varies depending on the load applied to the motor, attaining nearly 100% at maximum load. Stepper motor life is limited
only by the bearings and gears used. Stepper motors are typically capable of rotary speeds of around 1,000 rpm [123, 1995][118, 1998].

Continuous-rotation, brush-less DC motors are a suitable choice for the concentrator pointing mechanism as they provide high rotary speeds over a wide angular range and brushes do not limit their life. Although these types of motors behave in a DC fashion, they are actually Alternating Current (AC) synchronous motors with two or three phases. The motor rotates at speeds proportional to the frequency of the AC power applied to its terminals and inversely proportional to the number of poles it has. In order to make these motors behave in a DC fashion a position transducer is used which allows the drive electronics to adjust the applied AC signal to provide the required torque [123, 1995][118, 1998].

Given the complexities in powering a continuous-rotation, brush-less DC motor, it was considered adequate to employ brush DC motors for the purposes of ground testing.

To model a DC motor, we must consider the electric circuit of the motor and the motion of the rotor, as seen in figure (7.6).

\[
T = k_a I = k_a \frac{V}{R} \quad \text{(7.1)}
\]

where \(k_a\) is the armature constant. As the motor begins to rotate a back electromotive force (EMF) voltage is generated which opposes the applied voltage, causing the current to be reduced and therefore the generated torque. The back EMF(\(e\))\(^{29}\) is related to the rotational angular velocity by:

\[
e = k_i \dot{\theta} \quad \text{(7.2)}
\]

\(^{29}\) Back emf is a voltage which opposes the applied voltage to the motor and is produced by the inductance of the motor.
where \( k \) is the motor constant. For a constant applied voltage the torque delivered by the motor will be related to the motor's speed as seen in figure (7.7).

![Figure 7.7: Motor output torque vs. rotor speed][125]

From figure (7.7) it is clear that the motor will exhibit its maximum torque (stall torque) at very small motor speeds and will exhibit near zero torque at its maximum speed (no-load speed). Therefore, it is important when selecting a motor to ensure that the motor can deliver the necessary load torque at the required speed [125, 2000].

### 7.1.3 Classical Control System Design

In this section, a few essential concepts of classical control system design are introduced. In this work, frequency domain control techniques are employed; therefore, it is prudent to define the "Laplace Transform" and the "Transfer Function". The basic Laplace transform \( F(s) \) of a time signal \( f(t) \) is defined as:

\[
F(s) = \int_0^\infty f(t)e^{-st} dt \quad (7.3)
\]

and is written symbolically as:

\[
F(s) = L[f(t)] \quad (7.4)
\]

Where \( s \) is referred to as the complex frequency, sometimes written as \( s = j\omega \), where \( j \) is the square root of \(-1\) and \( \omega \) is the signal frequency. Laplace transforms are used to replace time domain differential equation models of systems with frequency domain algebraic equation models of systems; greatly simplifying the mathematics involved [125, 2000]. The transfer function of a system describes how the input to a system or system element is transferred to the output of the system or system element. For a system having a single input \( u(t) \) and a single output \( y(t) \), the transfer function \( T(s) \) model of a system is defined as the Laplace transformed output \( Y(s) \) divided by the input \( U(s) \), such that [125, 2000]:

---

[125]: #Reference to Figure or Content

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\[ T(s) = \frac{Y(s)}{U(s)} \quad \ldots \quad (7.5) \]

For example the transfer function of a motor can be found from Kirchhoff’s and Newton’s laws [125, 2000] along with equations (7.1) and (7.2) as:

\[ \frac{\dot{\phi}}{V} = \frac{k_a}{(Js + b)(Ls + R) + k_a k_i} \quad \ldots \quad (7.6) \]

Where \( J \) is the rotor inertia. The transfer function describes the response of the motor to a given input. In this case, for a given applied voltage, the motor transfer function predicts the motor angular velocity, taking into account friction and angular momentum.

Using block diagrams it is easy to visualise what a system would look like under feedback control. In figure (7.8) signals between system elements are presented with lower case symbols and system element transfer functions are presented with upper case symbols:

\[ \text{Sensor} \]

**Figure 7.8: Closed-loop single input, single output system. [126]**

In figure (7.8) the “Plant” \((P(s))\) refers to the system or system element (for example a motor) being controlled. The “Controller” \((C(s))\) adjusts the input to the plant \((u(s))\) based on the actuation signal \((a(s))\). External disturbances to the Plant are introduced through the addition of the signal \(d(s)\) to the Plant output \((v(s))\) to produce the system response \(y(s)\). In figure (7.8) the system is said to be closed loop as the actuation signal results from the difference between the feedback signal \((b(s))\) and the input signal \((r(s))\). The “Sensor” \((K(s))\) measures the system response \((y(s))\) and produces the feedback signal \((b(s))\), for example a shaft encoder could measure a motor’s rotary speed. The actuation signal would then be the difference between the required motor speed \(r(s)\) and the sensor measured motor speed \(b(s)\), as according to figure (7.8).

With mechanical Plats monitored by electronic Sensors it is usually safe to assume that the transfer function of the Sensor \((K(s))\) is unity for the frequency response waveband of the Plant, allowing the Sensor transfer function to be ignored in system modeling.

It is now prudent to define the concepts and expressions used for defining the performance of feedback control systems, with reference to figure (7.8). The system response under feedback \((y(s))\) is given by:

\[ y(s) = C(s)P(s)e(s) + d(s) \quad \ldots \quad (7.7)[126] \]
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as \( a(s) = r(s) - y(s) \) \( \Rightarrow \) \( y(s) = C(s)P(s)[r(s) - y(s)] + d(s) \) \( \ldots \)(7.8)[126]

so that

\[
y(s) = \frac{C(s)P(s)}{1 + C(s)P(s)} r(s) + \frac{1}{1 + C(s)P(s)} d(s) \ldots \ldots (7.9)[126]
\]

**Loop Gain:** This is the transfer function describing the combined system of the Controller and Plant when the feedback link is severed. It is sometimes referred to as the loop transfer function of the system, and is described by the transfer function \( C(s)P(s) \), as seen in equation (7.10).

**Sensitivity Function \( S(s) \):** This describes the effects of the external disturbance \( d(s) \) on the controlled output \( y(s) \) and is represented by:

\[
S(s) = \frac{1}{1 + C(s)P(s)} \ldots \ldots (7.11)[126]
\]

For the controller to achieve good disturbance rejection the sensitivity function must be small for the actuation signal frequency range of interest. However, **Loop Gain will tend to zero at high actuation signal frequency**, leading to:

\[
\lim_{s \to \infty} |S(s)| = 1 \ldots \ldots (7.12)[126]
\]

Therefore demonstrating that at high frequencies the system is unable to compensate for disturbances.

**Complementary Sensitivity Function \( CS(s) \):** This function relates the controlled output to the desired output \( r(s) \) without the presence of disturbances, and essentially describes controllers capacity to track the actuation signal over time. It is represented by:

\[
CS(s) = \frac{C(s)P(s)}{1 + C(s)P(s)} \ldots \ldots (7.13)[126]
\]

Clearly, it is required for \( CS(s) \) to be equal to unity for the actuation signal frequency range of interest. However, for higher frequencies, the Loop Gain tends to zero resulting in:

\[
\lim_{s \to \infty} |CS(s)| = 0 \ldots \ldots (7.14)[126]
\]

Therefore demonstrating that at high frequencies the system is unable to track the actuation signal over time.

### 7.1.4 Benefits of Fibre Optics

As discussed in chapter 4 the application of fibre optics can significantly reduce the mass of the concentrator system. In doing this, the moment of inertia of each concentrator in the system is also reduced. The moment of inertia of a concentrator mirror can be estimated by considering the moment of inertia of a disk with an axis of rotation about its central diameter.
Equation (7.15) describes the moment of inertia ($J$) of the disk to be dependant on the mass ($M$) of the disk multiplied by the radius ($R$) squared, however in equation (4.6) it was shown that the mass of the mirror is dependant on the fourth power of the radius. Newton's law gives the torque ($T$) needed to rotate the disk about the tilt axis as a function of angular acceleration ($\alpha$) ($T=Ja$). Given the torque required and therefore the power needed for tracking and disturbance, attenuation can be shown to significantly reduce by employing multiple concentrators.

The spacecraft's sun sensors if available would provide the mechanism pointing control system with an estimate of the sun-vector. Sun sensors, however, are unable to track the solar image on the focal plane. This is desirable as it allows for a direct closed-loop pointing control system that could ensure accurate solar tracking and angular disturbance compensation. It is here that a novel concept that can solve this problem is introduced.

![Figure 7.9: Moment of inertia of a disk. [127]](image)

$$J = \frac{1}{4} MR^2 + \frac{1}{12} ML^2 \quad \ldots \ldots \quad (7.15)$$

![Figure 7.10: Concentrator pointing determination and control concept.](image)
The concept is shown in figure (7.10) and consists of a bundle of seven fibre optics. Each fibre passes through a small enclosure in which a photo-sensor (photodiode) is attached to its side, a certain distance from the concentrator end of the bundle. The photodiodes measure the photoluminescence and light leakage emitted by each fibre resulting from the high intensity light traversing the fibre. The signals from the photo-sensors are interpreted by a pointing determination system to provide the position of the solar image on the fibre bundle tip. This feedback information provides a direct closed-loop pointing control system allowing the mechanism pointing actuation system to track the Sun and compensate for spacecraft angular disturbances. However, in order to use this sensor the concentrator must already be pointing at the Sun, to an accuracy of ±0.5°, such that the concentrator solar image lies on the fibre bundle tip. This requires a solar concentrator pointing acquisition system to provide an initial pointing of the concentrator. This could be achieved by the host satellites attitude system, combined with a Sun acquisition search algorithm. However, this research focuses on the fibre optic sensor only.

![Diagram of Pointing Concept System Architecture](image)

Figure 7.11: Pointing concept system architecture

Figure (7.11) depicts the closed-loop control system for this concept. The angular range of the fibre bundle is limited; such that a search algorithm is required for Sun acquisition after a coarse acquisition is made based on the initial estimate of the Sun vector.

If this pointing control technology could be implemented, then a further novel concept could also be realized, this being optical switching of the concentrated solar energy. Instead of using a bundle of only 7 fibres, a more complex bundle could be manufactured as shown in figure (7.12).

![Diagram of 3-Way Fibre Bundle Optical Switching Concept](image)

Figure 7.12: 3-Way fibre bundle optical switching concept

In figure (7.12) the bundle for the optical switching concept consists of 12 fibres with the three most central fibres being the energy transmitting fibres. Pointing of the concentrator can be
adjusted, via the pointing control system, such that the location of the solar image can be *switched* between these three central fibres allowing the transmitted power from one concentrator to travel to three separate receivers. For example, in figure (7.12), one fibre goes to a direct-gain receiver whilst another goes to a thermal-storage receiver and the third goes to an attitude control receiver. However, whilst this concept adds greatly to the versatility of the system it would also result in further inefficiency, as the small misalignment of the concentrator focal axis with the Sun vector required to locate the solar image with one of these fibres would result in a coma effect, which would enlarge the solar image. This is discussed in section 4.3.2.

### 7.2 Pointing System Algorithm Modelling

There are several approaches for developing a tracking algorithm for the pointing mechanism. The premise of the problem is to obtain Cartesian \((x,y)\) position telemetry of the centre of the solar image on the focal plane/fibre optic tip, as seen in figure (7.13).

It should be noted that the original sensor concept did not involve measuring fibre luminescence; rather it involved directly measuring the transmission of the surrounding fibres, which were assumed to be highly attenuating. This meant that only six fibres were available to provide spot telemetry rather than seven.

Consider a concentrator solar image (spot) trained on the end of a fibre optic cable forming a spot with an intensity distribution covering a finite area. The size of this spot is dependant on the concentration ratio of the primary parabolic mirror and the location of the fibre bundle on the focal axis, and it is required to control the concentrator pointing so that the spot is aligned with the fibre. To achieve this the fibre is surrounded by six others and each fibre’s luminescence (see figure (7.13)), when transmitting concentrated solar energy, is measured via photodiodes. It should be noted that the \(y\) direction is referred to as the “*elevation*” direction (or elevation channel) and the \(x\) direction is known as the “*azimuth*” direction (or azimuth channel). It is
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assumed that x and y axes are accurately aligned with the azimuth and elevation directions of a pointing mechanism respectively. For the real sensor it will be necessary to accurately orientate the fibre bundle such that the correct sensor response for motion in the azimuth and elevation directions is observed before the sensor can be employed for pointing control.

7.2.1 Exact Pointing Algorithm

A pointing algorithm, which is potentially very accurate, makes the assumption that the spot can be considered as a circle, which at the optimal concentrator concentration ratio is the same size as the fibres in the bundle, and that the spot lies within twice the radius of the central fibre. Furthermore, as only small angular movements of the concentrator can be tolerated (and therefore small displacements from the origin of the x, y plane of the fibre tip) the intensity distribution of the spot remains fixed, which for large angular movements wouldn't be the case, because of concentrator coma. This algorithm makes use of the consideration of two overlapping circles.

Consider a concentrator solar image (spot) trained on the end of a fibre optic cable. The spot is the same dimensions as the fibre optic, and it is necessary to control the concentrator pointing so that the spot is aligned with the fibre.

To achieve this the fibre is surrounded by six others and the radiative power in all seven fibres (see figure (7.14)) is measured. The light intensity is initially assumed to be just proportional to the area of overlap with the spot, and from this the position of the spot centre can be determined.

![Figure 7.14: Fibre bundle tip with concentrator spot.](image)

Figure 7.14: Fibre bundle tip with concentrator spot.

Consider a concentrator solar image (spot) trained on the end of a fibre optic cable. The spot is the same dimensions as the fibre optic, and it is necessary to control the concentrator pointing so that the spot is aligned with the fibre.

To achieve this the fibre is surrounded by six others and the radiative power in all seven fibres (see figure (7.14)) is measured. The light intensity is initially assumed to be just proportional to the area of overlap with the spot, and from this the position of the spot centre can be determined.

![Figure 7.15: Overlapping circles.](image)

Figure 7.15: Overlapping circles.
It should be noted that if the centre of the spot moves beyond twice the fibre radius \((2R)\) from the centre of the bundle the same power in the surrounding fibres can be observed as when the spot is over the central fibre. Hence to determine the position of the spot it is assumed that it lies within \(2R\) of the central fibre.

Consider two circles that intersect, both having the same radius \(R\). They intersect at two points (M and N (see figure 7.15)). Since \(OM=PM\), the triangle \(\Delta OMP\) is isosceles. MN is then the bisector of the angle \(\angle OMP\), and therefore intersects OP perpendicularly at the point L. From figure (7.15) the separation \((s)\) between the two circles is given by:

\[
OP = s = 2R \cos \theta \quad \text{(7.16)}
\]

The area of the segment \(OMT = (1/2) R^2 \theta\).

The area of the triangle \(\Delta OML = (1/2) R^2 \sin \theta \cos \theta\).

Therefore the area \(MLT = (1/2) R^2 \theta - (1/2) R^2 \sin \theta \cos \theta = (1/2) R^2(\theta - \sin \theta \cos \theta)\).

By symmetry the area of overlap \((A)\) is four times the area of \(MLT\).

\[
A = R^2(2\theta - \sin 2\theta) \quad \text{(7.17)}
\]

It should be noted here that if \(s\) is equal to \(2R\) then \(A\) is equal to zero. Furthermore, if \(s\) is equal to zero then \(A\) is equal to \(\pi R^2\), which is consistent with the circles lying on top of one another.

Equation (7.17) is consistent with the assumption that the radiative power intercepted by the fibre is proportional to the overlap area of the fibre \((P=IA\) where \(I\) is constant), which means that the intensity distribution of the spot is assumed to be the square profile discussed in section 4.3.2.

However, it has been shown that the intensity distribution of the spot is actually more Gaussian for real parabolic dish concentrators (section 4.3.2). To determine the power intercepted by the fibre it is necessary to integrate the spot intensity distribution over the fibre and spot overlap area.

\[
R > s > R
\]

(a) \(\theta > \phi > 0\)

(b) \(R > s > 0\)

Figure 7.16: (a) Integration boundaries for \(2R > s > R\). (b) Integration boundaries for \(R > s > \theta\).
To integrate the spot intensity distribution over the fibre overlap area it is necessary to identify the integration boundaries. From figure (7.16) it can be seen that the integration boundaries change depending on the separation \( s \) between the centre of the fibre and that of the spot. The parameter \( x \) describes the position of the fibre boundary with respect to the centre of the spot. From the law of cosines the relationship between \( x \) and the angle \( \phi \) can be found:

\[
R^2 = x^2 + s^2 - 2sx \cos \phi \quad \text{(7.18)}
\]

rearranging for \( x \): \[
x^2 - 2sx \cos \phi + s^2 - R^2 = 0 \quad \text{(7.19)}
\]

Equation (7.19) is a quadratic equation in \( x \) the solution to which is:

\[
x = s \cos \phi - \sqrt{(s \cos \phi)^2 - s^2 + R^2} \quad \text{(7.20)}
\]

With equation (7.20) the spot intensity distribution \( I(r) \) can be integrated over the fibre overlap area for varying values of \( s \).

For \( R > s > 0 \):

\[
P(s) = 2 \int_0^{\phi} \int_0^r I(r)r \, dr \, d\phi + 2 \int_0^{\phi} \int_0^{R} I(r)r \, dr \, d\phi \quad \text{(7.21a)}
\]

For \( 2R > s > R \):

\[
P(s) = 2 \int_0^{\phi} \int_0^{R} I(r)r \, dr \, d\phi \quad \text{(7.21b)}
\]

The two integrals in equation (7.21a) are necessary as for a portion of the integration over \( \phi \) the integration over \( r \) is up to the spot boundary whereas for rest of the integration over \( \phi \) the integration over \( r \) is bounded by the fibre boundary. The equation for a Gaussian function is:

\[
I(r) = \frac{1}{\sigma \sqrt{2\pi}} e^{-\left(\frac{r-\bar{r}}{2\sigma}\right)^2} \quad \text{(7.22)}
\]

where \( \sigma \) is the standard deviation. Equations (7.21) were evaluated numerically, using equation (7.22), to find the fibre intercepted radiative power for values of \( s \) ranging from 0 to 2R. The results of this are plotted in figure (7.17) for two values of \( \sigma \) (\( \sigma = 0.2 \), effective spot diameter 1mm and \( \sigma = 0.25 \), effective spot diameter 1.5mm experimentally estimated size of concentrator spot for the Cassegrain concentrator unit discussed in chapter 4 section 4.4.3) along with the results for the same analysis with a square spot intensity distribution and also normalised experimental data taken from figure (4.21) to demonstrate experimental results consistent with a \( \sigma = 0.25 \) Gaussian intensity profile.
Figure 7.17: Normalised fibre intercepted power vs. displacement for concentrator spot intensity profiles (Measured data error ±0.01W)

From figure (7.17) it is clear that a Gaussian intensity distribution results in fibre intercepted radiative power being less susceptible to small values of $s$ in comparison to the square intensity distribution. However, for larger values of $s$ the reduction in fibre intercepted power is more significant for the Gaussian distributions.

The solution to the inverse problem, however, is what is required in order to find the position of the spot with respect to the central fibre. For the square spot intensity profile $s$ can be found from equations (7.16 and 7.17). By considering the area of overlap of multiple fibre in the bundle the centre of the spot ($s_7, \varphi_7$) can be found, as shown in figure (7.18):

![Figure 7.18: Multiple fibre overlap geometry](image)

In figure (7.18) the distances $s_1$, $s_2$, $s_3$ and $s_7$ are determined from the overlap areas of the spot with fibres 1, 2, 3 and 7 respectively (from equations (7.16 and 7.17)). With these distances $\varphi_7$ can be calculated via the cosine rule (equation (7.23)). This algorithm was simulated by artificially moving the spot across the fibre tip in both the x and y directions.

$$\cos \varphi_7 = \frac{s_7^2 + (2R)^2 - s_i^2}{2s_i(2R)} \quad \text{...(7.23)}$$
In Figure 7.19(a) and (b), the estimated spot position is plotted against the actual spot position as the spot is moved over the fibre bundle tip for both the x and y (azimuth and elevation) coordinates. The intensity profile of the spot is Gaussian (with $\sigma = 0.2$, which results in an effective spot diameter of 1 mm). This is assumed to represent the concentrator spot at maximum theoretical concentration by capturing 99% of the power of the Gaussian intensity distribution within the fibre area). In Figure 7.19 the difference between actual position and estimated position varies from the center of the fibre bundle, where the difference is small (<0.01 mm), to the edge of the fibre bundle, where the difference is larger. The maximum difference is observed at the actual spot position (±) 0.6 mm and is 0.25 mm in magnitude. The average difference, calculated over the fibre bundle diameter from Figure 7.19, for the azimuth and elevation estimates are ±0.1288 mm and ±0.1445 mm respectively. It should be noted that the discontinuity located at the origin of each plot would make it difficult for a controller to locate the spot exactly in the middle of the central fibre. The size of this discontinuity is 0.03 mm, which according to Table 7.1 represents a 2.5% decrease in coupling efficiency, which in turn is within the tolerable decrease in coupling efficiency of 5%. Therefore in practice this method is usable, although the actuation (a dc motor) for each axis of motion (azimuth and elevation) will constantly be switching from one side of the discontinuity to the other, which may result in increased power consumption and induced system vibrations. With precise knowledge of the spots spatial intensity profile it was found that the algorithm could be made to be much more accurate, with maximum errors of ±0.001 mm in magnitude, see Figure 7.20. This is accomplished by using the known spots spatial intensity profile as a reference to more accurately estimate the distance between the spot and a particular fibre. In particular, this is achieved by employing the relationship shown in Figure 7.17, which is the power intercepted by the fibre area as a function of distance between the spot and fibre (calculated from the spot intensity distribution, equation (7.22)), in place of equations (7.16 & 7.17) to find $s$ from the fibre intercepted power which is assumed to be proportional to $A$. This
A second pointing algorithm that sacrifices accuracy for simplicity, less computation and robustness was developed. The reason for the development of this pointing algorithm was to reduce computation during real-time feedback control of the concentrator and make implementation of the sensor easier. This algorithm splits up the fibre bundle into quadrants A, B, C and D as shown in figure (7.13). This is analogous to laser pointing sensors in DVD players [128, 2006] and the quad-cell Sun pointing sensor of the PICARD SODISM instrument [40, 2003], see Figure (2.12). Consider a quad-cell sensor that is capable of separately measuring incident radiative power \((P_A, P_B, P_C, P_D)\) in the four quadrants A, B, C and D, as shown in figure (7.21).
position of the spot we compare the powers in the four quadrants for a spot which is displaced from the origin of the sensor, located at \((x_0, y_0)\).

![Diagram](image)

**Figure 7.22:** (a) Geometry for x coordinate calculation, (b) Geometry for y coordinate calculation.

From figure (7.22) the sum of the powers \(P_A + P_B, P_C + P_D, P_A + P_D\) and \(P_B + P_C\) can be calculated from equation (7.17):

\[
\begin{align*}
P_A + P_B &= \pi R^2 - \frac{1}{2} R^2 (2\theta_1 - \sin 2\theta_1) \quad \text{......(7.24a)} \\
P_C + P_D &= \frac{1}{2} R^2 (2\theta_1 - \sin 2\theta_1) \quad \text{......(7.24b)} \\
P_A + P_D &= \pi R^2 - \frac{1}{2} R^2 (2\theta_2 - \sin 2\theta_2) \quad \text{......(7.24c)} \\
P_B + P_C &= \frac{1}{2} R^2 (2\theta_2 - \sin 2\theta_2) \quad \text{......(7.24d)}
\end{align*}
\]

where
\[
\theta_1 = \cos^{-1} \left( \frac{x_0}{R} \right) \quad \text{......(7.25a)} \quad \text{and} \quad \theta_2 = \cos^{-1} \left( \frac{y_0}{R} \right) \quad \text{......(7.25b)}
\]

Subtracting (7.24a) from (7.24b):

\[
(P_A + P_B) - (P_C + P_D) = \pi R^2 - R^2 (2\theta_1 - \sin 2\theta_1) \quad \text{......(7.26a)}
\]

Subtracting (7.24c) from (7.24d):

\[
(P_A + P_D) - (P_B + P_C) = \pi R^2 - R^2 (2\theta_2 - \sin 2\theta_2) \quad \text{......(7.26b)}
\]

Substitution of equations (7.25 a and b) for angles \(\theta_1\) and \(\theta_2\) in equations (7.26 a and b) and dividing by the total power in the spot gives the following relations:

\[
\begin{align*}
\frac{(P_A + P_B) - (P_C + P_D)}{P_A + P_B + P_C + P_D} &= \frac{2}{\pi} \sin^{-1} \left( \frac{x_0}{R} \right) + \frac{2x_0}{\pi R^2 \sqrt{R^2 - x_0^2}} \quad \text{......(7.27a)} \\
\frac{(P_A + P_D) - (P_B + P_C)}{P_A + P_B + P_C + P_D} &= \frac{2}{\pi} \sin^{-1} \left( \frac{y_0}{R} \right) + \frac{2y_0}{\pi R^2 \sqrt{R^2 - y_0^2}} \quad \text{......(7.27b)}
\end{align*}
\]
The quantities on the right hand side of equations (7.27 a and b) are functions of \( x_0 \) and \( y_0 \) respectively. For small values of \( x_0 \) and \( y_0 \), compared to \( R \), equations (7.27 a and b) reduce to:

\[
\frac{(P_A + P_B) - (P_x + P_B)}{P_A + P_B + P_C + P_D} = \frac{4x_0}{\pi R} \quad (7.28a)
\]

\[
\frac{(P_A + P_D) - (P_E + P_C)}{P_A + P_B + P_C + P_D} = \frac{4y_0}{\pi R} \quad (7.28b)
\]

When \( x_0 \) and \( y_0 \) are equal to \( R \), such that the spot is completely within the upper right hand side of the sensor, the values of the quantities on the right hand side of equations (7.27 a and b) are unity and will be non-real for larger values of \( x_0 \) and \( y_0 \). Between these two extremes the quantity varies as shown in figure (7.23a). This demonstrates that algebraic comparison of the measured powers of the quadrants can be representative of the Cartesian position of the spot for feedback control.

Consider now the fibre optic bundle to act as a quadrant sensor. Photodiodes measure the power intercepted by each fibre \( (P_I - P_J) \). The bundle is then divided into the quarters A, B, C and D, as shown in figure (7.23b):

![Figure 7.23: (a) Variation of equation (7.27a) for R = 1. (b) Seven fibre optic bundle as a quadrant sensor.](image)

then the power in each quadrant is given by:

\[ P_A = P_1 + \frac{1}{2} P_1 + \frac{1}{4} P_7 \quad \ldots \quad (7.29a) \]

\[ P_B = P_3 + \frac{1}{2} P_4 + \frac{1}{4} P_7 \quad \ldots \quad (7.29b) \]

\[ P_C = P_5 + \frac{1}{2} P_4 + \frac{1}{4} P_7 \quad \ldots \quad (7.29c) \]

\[ P_D = P_6 + \frac{1}{2} P_1 + \frac{1}{4} P_7 \quad \ldots \quad (7.29d) \]

where it is necessary to apply the factors of \( \frac{1}{2} \), to fibre 1 and 4, and \( \frac{1}{4} \), to fibre 7, to correctly represent the powers in each quadrant. For example, the power in quadrant A consists of all the radiative power intercepted by fibre 2, half of the power intercepted by fibre 1 and a quarter of the power intercepted by fibre 7.
From equations (7.29a-d) the estimation of the Cartesian location \((x, y)\) of the spot on the fiber bundle tip in the \(x\)-\(y\) plane can be estimated by comparing the powers of the bundle quadrants A, B, C and D as according to equations (7.28):

\[
\begin{align*}
    x &= \frac{(P_A + P_D) - (P_C + P_B)}{P_A + P_B + P_C + P_D} \quad \text{(7.30a)} \\
    y &= \frac{(P_A + P_D) - (P_B + P_C)}{P_A + P_B + P_C + P_D} \quad \text{(7.30b)}
\end{align*}
\]

Using the fiber bundle as a quadrant sensor is more complicated than the previously considered quadrant sensor case as the gaps in between the fibers represent areas where the incident power cannot be measured. Also, due to the fiber geometry some fibers are located on the Cartesian axes which will result in a coupling effect between the estimated \(x\) and \(y\) coordinates. To demonstrate this, the fiber bundle sensor was numerically simulated with a spot the same size as a fiber optic and with square intensity profile. This spot was moved to different locations on the fiber bundle.
tip and the x and y coordinates estimated by equations (7.30) were recorded. Figure (7.24a and b) plots the estimated x and y position of the spot as the spot is moved along the x and y axes respectively. Figure (7.24) demonstrates that equations (7.30) can be used to relatively accurately describe the motion of the spot along the Cartesian coordinates frame axes, and this represents the best accuracy attainable via this method. The method accuracy decreases, however, as the spot is moved away from the x and y axes. This is due to a coupling between the x and y coordinates. This coupling effect is shown graphically in figure (7.25) where figure (7.25a) is a contour plot of the estimated x position for varying actual y position and figure (7.25b) is a contour plot of the estimated y position for varying actual x position. It is clear from figure (7.25), and especially for figure (7.25b), that the method's accuracy decreases with distance from the coordinate origin. Despite this, the method is still able to quickly provide an estimate of the spot position.

To see the effect of a gaussian spot intensity profile the same analysis as described above was conducted, but with the spot having a gaussian distribution. This was done for two sizes of distribution, one being the ideal distribution size ($\sigma = 0.2$, effective diameter 1mm) and the other being a slightly larger ($\sigma = 0.25$, effective diameter 1.5mm) distribution size, more representative of the real spot obtainable in a laboratory environment, as identified in section 4.4.3.

![Figure 7.26 (a): Azimuth error graph for ABCD pointing algorithm. (b): Elevation error graph for ABCD pointing algorithm.](image)

In Figure (7.26) the estimated position (for both distribution sizes) of the spot is plotted against the actual position for motion along the x and y axes respectively. This demonstrates the accuracy and sensitivity of the algorithm to differing spot radii. For very small spot sizes, which are much smaller than that of the fiber, the controllability of the pointing system becomes a complicated non-linear matter. On increasing the size of the spot, controllability of the fiber is more linear, however, this is at the expense of power input as for much larger spots, some light will fall on the gaps between the fibers. The average errors for the ideal spot x and y plots are ±0.1301mm and ±0.1408mm. The average errors for the real spot x and y plots are ±0.0752mm and ±0.0826mm.
However, it should be noted that for spot positions away from the coordinate frame axes, larger errors would be visible, indicative of figure (7.25).

The ABCD algorithm was chosen for practical testing due to its ease of implementation, reduced computation and signal quality robustness, as relatively little signal interpretation is required. However, it is recommended that the exact algorithm be investigated further to improve the accuracy of the sensor.

### 7.3 Concentrator Pointing System Hardware Development

This section describes the efforts undertaken to practically develop and experimentally demonstrate the fibre optic solar concentrator tracking technology discussed in section 7.1.4. The initial investigation was to establish the behaviour of the silica fibre optic luminescence effect observed in previous experiments. Using this effect, a prototype sensor was constructed and tested. Then a concentrator pointing mechanism was designed, constructed and characterised. Finally the sensor was employed to demonstrate closed loop pointing control of the concentrator pointing mechanism.

#### 7.3.1 Investigation of Silica Fibre Optic Luminescence

The aim of this investigation was to assess the behaviour of the luminescence exhibited by the FLU fibre optic when transmitting concentrated sunlight. This effect was observed in the experiments discussed previously in chapters 4, 5 and 6 and is shown in figure (7.27).

![Figure 7.27: FLU fibre luminescence when transmitting concentrated light from a solar simulator.](image)

The investigation focused on two areas:

- Variation in luminescence intensity of the fibre at a particular point on the fibre with changing fibre transmitted power.
• Variation in luminescence intensity of fibre over the fibre length for a fixed fibre transmitted power.

In these investigations the main focus of the experiments was the variation in the luminescence intensity and the resulting variation in photo-sensor response, rather than any spectral variation. To assess the behaviour of the fibre photoluminescence, a photo-sensor was needed which was small enough to be placed close to the fibre in order to measure the luminescence of a small section of the fibre. The emission spectrum of the fibre luminescence was not available for photo-sensor selection, and the only knowledge of the emission spectrum of the fibre luminescence was that a strong blue colour was observed. It was therefore necessary to choose a photo-sensor with a broad spectral responsivity, covering the UV to near-IR waveband. The photo-sensor selected for this application is the OPT101P photodiode, a product of Burr-Brown. This particular photodiode has an inbuilt transimpedance amplifier, a bandwidth of 14kHz, a sensor area of 5.2 mm², exhibits good linearity and has a broad spectral responsivity as seen in figure (7.28c) [129, 2005]. These photodiodes are commonly found within medical and laboratory instrumentation [129, 2005]. A single OPT101P photodiode was integrated into the circuit shown in figure (7.28a):
In the same manner as the experiments designed to investigate the intensity distribution of a parabolic concentrator were conducted, see figure (4.20), a 105mm diameter parabolic concentrator is used to focus the beam of a solar simulator onto the tip of a 1mm core diameter, 2m long FLU fibre optic cable. Both the concentrator and fibre optic cable were located via an optical rail, with micrometer stages in place to control their relative positions. The optical rail and micrometer stages are employed at first to locate the fibre tip precisely at the focal point of the concentrator. At the focal point, the fibre transmitted power was measured using an Oriel thermopile sensor, and found to be ~4.5W, as was determined previously and discussed in chapter 4, section 4.4.3. Then by moving the fibre tip in a radial direction from the concentrator focal point, across the concentrator focal plane, the fibre transmitted power can be changed. The OPT101P photodiode is placed next to the fibre optic cable 1.5m along the length of the fibre from the concentrator end of the fibre. It is placed in an enclosure to ensure no background light impinges upon the photodiode. Upon changing the fibre transmitted power the photodiode response voltage, due to the fibre optic luminescence, is recorded using a digital multi-meter. The results of this test are shown in figure (7.29).

In figure (7.29) a linear relationship is observed between fibre transmitted power and the photodiode response and repeated tests yielded the same results. This confirmed the assumed linear relationship employed in the pointing algorithm analysis of section 7.2. Unfortunately this linear relationship could only be confirmed up to a fibre transmission power of ~4.5W. However, it is thought that with the cause of this luminescence being primarily due to photoluminescence and non-perfect total internal reflection and given the nature of these two processes it is not an unreasonable assumption that the linear relationship continues for higher fibre transmission powers. Furthermore, as this linear relationship has been observed for such a wide range of fibre optic power transmission values, it is argued that any non-linear behaviour for higher fibre transmission powers would not significantly affect the ABCD tracking algorithm, due to its use of
the surrounding fibres in the bundle for spot position location. It could also be argued that any non-linear relationship could be compensated for via an appropriate controller. Future tests would need to confirm this linear relationship to higher fibre transmission powers, and it may be necessary to choose a different photo-sensor capable of responding to a larger range of luminance without becoming saturated or change the gain of OPT101P photodiode response. For the purposes of simplicity in the continuing research, however, this linear relationship was assumed to hold to higher fibre transmission powers.

To establish the behaviour of the fibre luminescence with varying positions along the fibre, the fibre tip of a 18m long fibre was located at the concentrator focal point. Then the enclosure, containing the fibre and photodiode were moved to different points along the fibre length, ~0.5m apart and the fibre luminance measured. The results of this test are shown in figure (7.30).

![Photodiode voltage vs. Photodiode position along fibre.](image)

Figure (7.30) indicates an observed trend for fibre luminescence intensity to decrease as the photodiode is positioned further from the concentrator end of the fibre. However, there is a significant variation in photodiode response between the 8 and 13 m photodiode positions. It is unclear why this is the case. A possible explanation for the observed data could be experimental errors, which could include a variation in background lighting conditions in the solar simulator laboratory. Another explanation for the observed data could be that the exhibited fibre luminance is dependant on local fibre properties, such as local core contaminant concentrations, damage to the core surface (scratches or micro bends) and physical (macro) bends of the fibre. It is clear that in order to ensure effective comparison between the fibre luminescence of each fibre in a bundle, photodiode signal normalisation is required, even if each photodiode is located the same distance away from the concentrator along the fibre.
7.3.2 Fibre Optic Concentrator Pointing Determination Sensor Testing

Having established the relevant behaviour of the fibre optic luminescence, as measured by a photodiode sensor, the fibre optic concentrator pointing determination sensor could be manufactured. A bundle of seven fibres was constructed using high temperature epoxy resin (see figure (7.31a)), and an array of 7 photodiode sensors, powered in a series circuit, was manufactured (see figure (7.31b)).

7.3.2.1 Preliminary Testing of First Manufactured Fibre Sensor

A preliminary test was conducted, again using the experimental set up of figure (4.20), although the seven-fibre sensor bundle was located at the concentrator focal point instead of a single fibre. Each fibre in the bundle was passed through an enclosure box, located ~1.5m along the fibre bundles length, and was coupled to a separate photodiode in the photodiode array. An initial calibration of the sensor was required. This was done, by measuring each photodiodes response voltage when its corresponding fibre optic was located at the focal point and, therefore, receiving maximum illumination. This maximum photodiode response allowed the photodiode signals to be subsequently normalised. It should be noted that these measured calibration values, although expected to be similar, were found to vary substantially. The cause of the variation in calibration values is identified in the next section.

Using a micrometer stage the fibre bundle was manually placed at different locations with respect to the concentrator spot image on the azimuth axis of the fibre bundle (see figure (7.13)). At each location, all seven normalised photodiode responses were taken using a digital multi-meter. The ABCD pointing algorithm was then used to manually compute the estimated position of the fibre with respect to the centre of the concentrator spot at each location. Figure (7.24) shows the error curve of this preliminary sensor test:
In Figure (7.32), the fibre optic sensor error curve is plotted (actual position vs. estimated position). Although it appeared that the sensor successfully tracked the sign of the actual location, the gain and shape of the error curve was not in agreement with the simulated ABCD error curve of figure (7.26). The error for the estimated position is based on a photodiode response error of ±10mV.

7.3.2.2 XPC-Target Testing of First Manufactured Fibre Sensor

To investigate these inconsistencies and develop the fibre optic sensor further, a faster, more elaborate sensor test was required. To increase the speed of analysing the fibre optic sensor tests, a computer data acquisition system was employed to monitor the photodiodes output voltage response. The data acquisition system used was XPC-Target, which operates from within Matlab/Simulink. A simple simulink system was set up to monitor the seven photodiodes and log their output voltage responses (Figure (7.33)).

Figure 7.32: Preliminary fibre optic sensor error curve.

Figure 7.33(a): XPC-Target system. (b) XPC-Target photodiode data logger.
A “Host” PC acts as the user interface for creating instructional code for the hardware interface. This code is uploaded to a “Target” PC running a kernel, which interfaces with the hardware (which in this case is the photodiode array) via a National Instruments 6024E PCI card. The data logged by the XPC-Target system was sampled at 1000Hz and then processed by a Matlab script to produce normalised photodiode responses and concentrator spot position data.

It was at this point in the pointing system development that a two-axis (azimuth and elevation) concentrator pointing mechanism became available. This pointing mechanism is shown in figure (7.34), and was designed specifically for the concentrators manufactured for this research. The development of this concentrator pointing mechanism will be discussed in section 7.3.3.

The concentrator pointing mechanism allowed faster motion of the fibre over the concentrator spot, although no actual position data could be logged. However, using the XPC-Target system with the solar concentrator pointing mechanism would enable fast assessment of the relative photodiode responses. A new fibre optic concentrator connector was manufactured to accommodate for the larger size of the fibre optic bundle, in comparison to the single fibre that was originally coupled to the concentrator.

![Figure 7.34: Solar concentrator pointing mechanism.](image)

A sensor test was formulated to determine if the fibres in the bundle were responding in the correct manner, relative to one another. The tests involved moving the spot over the fibre tip from top to bottom (see figure (7.35b)). The desired response of the photodiodes is shown in figure (7.35a).
The purpose of this test was to look for any photodiode signal behaviour that may deviate from the desired response of figure (7.35a). The differences in magnitude of response for photodiodes 2, 3, 5 and 6 allow for the fact that the concentrator spot may not move exactly along the y-axis. Deviations from this desired response could be in the following forms:

- Bunched together photodiode responses: This could indicate cross coupling of light from one fibre to a neighbouring fibre via a break down in the fibre cladding. This could result from poor fibre optic preparation. The fibre bundle was prepared by removing the buffer material from each fibre and then hand polishing each fibre with several grades of lapping film. The bundle was then assembled using a high temperature epoxy resin. The cladding of the FLU fibre is Teflon, this was found to be particularly susceptible to stress once the buffer was removed and would crumble easily. Therefore care in fibre bundle preparation was needed.

- Photodiode responses appearing to act before they should: This could indicate that the fibres in the bundle have either:
  - Significantly varying numerical apertures. This would also cause the non-normalised photodiode responses to be significantly different.
  - Bundle geometry was not correct. The fibres in the bundle were different distances away from each other.

- Photodiode signal noise: The level of noise in the photodiode signal is indicative of the size of the non-normalised response of the photodiode. Significantly differing signal noise levels would again indicate variations in fibre numerical apertures.
It should be noted that the spot size and spatial distribution of power at the input to the fibre is independent of wavelength. Figure (7.36) shows the photodiode responses from conducting this test with the first manufactured fibre bundle.

![Figure 7.36: Photodiode response test results for first manufactured fibre bundle (±0.02 normalised). The concentrator spot is moved along the elevation axis of the fibre bundle and the photodiode response to each fibre's luminescence is logged.](image)

In figure (7.36) the first item of note is the level of noise in each signal. All signals, apart from photodiode signal 3, have a large amount of signal noise. This could be indicative of degradation of the fibre's numerical aperture, although other sources of signal noise could not be ruled out at the time. This signal noise also seems to cause the photodiodes responses to have large non-zero values when the fibre is not illuminated. The second item of note is that the responses are very bunched together, which could indicate a degree of fibre cross coupling.

Using the signal test data in figure (7.36) the estimated spot position over the course of the test was calculated via the ABCD algorithm (see figure (7.37)). This was done in order to examine the behaviour of the position error signals. Also plotted was the ABCD power curve, this being the sum of all the photodiode responses at a particular location. The ABCD power curve is useful for determining control saturation levels and will be discussed further in section 7.3.4.

In figure (7.37) the most obvious failing is the level of noise in the position error signals, clearly resulting in non-zero position error values when the fibre is not being illuminated. Although the sensor appears to track the sign (+-) of the actual spot position quite well, this sensor was deemed unsuitable to attempt closed loop control of the pointing mechanism and a new fibre optic sensor
was required. On examining the first fibre optic bundle after photodiode response testing, it was found that there had been a massive degradation of each fibre's numerical aperture, most probably incurred during fibre bundle manufacture. Indeed, all fibres apart (from fibre 3) demonstrated large decreases in transmission efficiency. It was clear that the non-professional assembly of the original fibre bundle had resulted in significant fibre damage. It was also found that appreciable signal noise was also being introduced in the ribbon cable between the photodiode sensors and "Target" Pc.

![Diagram](image)

Figure 7.37: ABCD calculated position for first sensor photodiode response test (±0.04mm).

7.3.2.3 XPC-Target Testing of Second Manufactured Fibre Sensor

In making a new fibre bundle, a lot more care was taken and acceptable fibre transmission efficiencies were confirmed before conducting the photodiode response tests. The ribbon cable noise was also taken into account. With the new fibre, the same photodiode response tests as for the first seven-fibre sensor were conducted, and the results are shown in figure (7.38). In figure (7.38), it is clear that almost all signal noise has been eliminated, and the fibre responses are more in agreement with the desired response of figure (7.35a). The shape of fibre response curves in figure (7.38a), in comparison to the fibre response curves in figure (7.38b) indicate that the motion of the concentrator spot over the fibre bundle tip was not uniform. However, what is most important about the fibre responses in figure (7.38a) is the relative position of the peaks of the fibre responses, which are consistent with those desired in figure (7.38a). Furthermore, it was found that fibre calibration values were now very similar. Using the photodiode response test data, the estimated spot position over the course of the test was calculated via the ABCD method.
Fibre Optic Applications in Solar Thermal Propulsion Systems

(see figure (7.39)). In figure (7.39), the error signals are much cleaner and provide much better non-illuminated responses. Although it should be noted here that the azimuth error signal may either indicate that the fibre bundle tip is slightly tilted to one side or a small amount of cross coupling is occurring between the azimuth and elevation directions.

![Graph](image)

Figure 7.38: (a) Photodiode response test results for second manufactured fibre bundle (±0.02 normalised). (b) Desired bundle response.

![Graph](image)

Figure 7.39: ABCD calculated position for second sensor photodiode response test (±0.04mm).

Theses tests were deemed successful and the sensor was approved for attempting closed loop control of the concentrator pointing mechanism.

A further test was performed for the new sensor to assess the relation between the estimated spot position and actual spot position. This test was performed in the same manner as that done previously for the first sensor (see figure (7.32)). However, in this case the XPC-Target system was employed to measure photodiode responses and display the estimated position of both azimuth and elevation channels enabling faster data acquisition. More data was taken for this test,
as initially better agreement was observed between measured estimated positions and simulated estimated positions.

In figure (7.40a & b), the estimated sensor relations between actual spot position and measured spot position can be seen for both the azimuth and elevation directions. Both estimated relations exhibit similar responses to the simulated relations shown in figures (7.26a & b). Although, the
relations shown in figure (7.40a & b) are occasionally flatter than those in figure (7.26a & b), which could be attributable to a slightly larger concentrator spot size or the spot intensity profile not being exactly Gaussian. Other sources of error may include error in fibre response calibration, and the non-perfect elimination of back ground light in the fibre optic sensor box. However, these tests demonstrated that the new fibre optic bundle sensor was functioning satisfactorily and was relatively consistent with predicted behaviour.

### 7.3.3 Pointing Mechanism Characterisation

![Image](a)

![Image](b)

![Image](c)

Figure 7.41(a): Pointing mechanism concentrator mounting. (b) SolidEdge modelling of pointing mechanism mechanical design.

For the purposes of ground testing and concept proving, a pointing mechanism was designed and constructed. The pointing mechanism was designed to be an elevation-azimuth style pointing system that was of the correct size to steer the Cassegrain concentrator unit described in section 4.2 (figure (7.41c)). Two 24V DC brushed motors drive each axis of revolution. Each motor has a
gearbox such that the overall gear ratio for each motor is 50:1, providing an axel no load speed of 108rpm (figure (7.41a)). The design of the mechanism structure was modelled in Solid Edge, a computer aided design analysis tool, to determine the structures moments of inertia about the relevant axes of revolution (Figure 7.41b). This allows modelling of the motor speed which aided motor selection. In figure (7.34) the constructed mechanism is shown. The final system component required is the motor drive amplifier circuit. The motor drive circuit selected for this application was the MD22 Dual 5Amp H-Bridge switching motor drive circuit, a product of Devantech Ltd. The MD22 has the advantage that it can drive two DC motors independently and has a switching frequency of 15kHz, thus making full use of the photodiode bandwidth (14kHz). The input signals accepted by the MD22 must have a 2.5 V offset.

Figure 7.42(a): Experimental azimuth frequency response with system model. (b) System model bode diagram.

For the design of a control algorithm, a detailed model of the open-loop system is required. This detailed model is required in the form of the system frequency response, which can be represented in the form of a Bode diagram. A Bode diagram graphically compares the amplitude and phases of the system input and system output as a function of input and output signal frequency. For example, figure (7.42b) shows a Bode diagram representing an initial estimation of the frequency response of the azimuth channel of the pointing mechanism. In figure (7.42b) the top plot gives the ratio of input and output signal amplitudes (measured in decibels) as a function of signal frequency. The bottom plot gives the phase difference between the input and output signals (measured in degrees) as a function of signal frequency. This initial estimation of the frequency response shown in figure (7.42b) is based on a mathematical model derived from equation (7.6) and based on the electromechanical parameters for the motor from its data specification and
moment of inertia values from the SolidEdge simulation of the mechanical design. These values are shown in table (7.2).

Table 7.2: Pointing system electromechanical model parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Azimuth Preliminary</th>
<th>Azimuth LVDT</th>
<th>Elevation LVDT</th>
</tr>
</thead>
<tbody>
<tr>
<td>Moment of inertia of mechanical system (J) (kgm²s⁻²)</td>
<td>0.00003</td>
<td>0.00003</td>
<td>0.000005</td>
</tr>
<tr>
<td>Damping ratio of mechanical system (b) (Nms)</td>
<td>0.04</td>
<td>0.0004</td>
<td>0.0008</td>
</tr>
<tr>
<td>Armature constant (kₐ) (NmA⁻¹)</td>
<td>0.0448</td>
<td>0.0448</td>
<td>0.0448</td>
</tr>
<tr>
<td>Motor constant (kₗ) (V/(rads·s)⁻¹)</td>
<td>0.0448</td>
<td>0.0448</td>
<td>0.0448</td>
</tr>
<tr>
<td>Motor electrical resistance (R) (Ω)</td>
<td>3.2</td>
<td>3.2</td>
<td>3.2</td>
</tr>
<tr>
<td>Motor electrical inductance (L) (H)</td>
<td>0.0832</td>
<td>0.0832</td>
<td>0.0832</td>
</tr>
<tr>
<td>Additional gain for LVDT (V/mm)</td>
<td>NA</td>
<td>5.6</td>
<td>8.9</td>
</tr>
</tbody>
</table>

\[
\theta = \frac{k_\phi}{s((Js + b)(Ls + R) + k_\phi k_\beta)} \quad \text{(7.31)}
\]

To validate the model some experimental identification was performed. The XPC target system was employed to control the motors via the MD22 motor drive amplifier circuit. The simulink model for this test is shown in figure (7.43).

Figure 7.43: Simulink single harmonic frequency response model.

Simulink, developed by The MathWorks, is a tool for modeling, simulating and analyzing multidomain dynamic systems.
By controlling the input to the azimuth motor using a harmonic signal with known frequency and peak-to-peak amplitude, the response in terms of angular displacements is manually measured for a set of six frequencies. These values are then superimposed over the mathematical model for assessment (Figure 7.42a). Although similar behaviour is observed between the mathematical model and observed data, this method of system open loop identification was found to be unsuitable for controller design. Because of the discrepancies between the mathematical model and the experimental data, a further identification of the open-loop model was proposed, utilising an LVDT proximity sensor. A device from Solatron was selected providing 10mm range with 99.7% of range linearity and 540(mV/mm) sensitivity. Using a swept input to the motor and collecting both input and output displacement through the LVDT sensor, Fourier analysis can be performed to experimentally derive the frequency response functions. These can be used for designing feedback controllers for both the azimuth and the elevation motions. The LVDT tests were conducted as seen in figure (7.44).

![LVDT test equipment setup](image)

![Simulink model for chirp signal generation](image)

Figure 7.44(a): LVDT test equipment setup. (b) Simulink model for chirp signal generation.
In figure (7.44a) it can be seen that the LVDT is set up to monitor fibre optic tip position during the testing. In figure (7.44b) it can be seen that a chirp signal is generated and used to command one of the pointing mechanism motors while the other remains fixed. The chirp signal amplitude is made to increase with time to ensure that frequency response data can be obtained well past the mechanism bandwidth frequency. Also in figure (7.44b) it can be seen that the LVDT response is logged for the duration of the test. Comparing the logged chirp signal with the logged LVDT response allows for the computation of the open loop mechanism frequency response. This was done for both the azimuth and elevation channels of the system output (figure (7.45)).

![Frequency response graphs](image)

**Figure 7.45(a):** LVDT azimuth frequency response. **(b)** LVDT elevation frequency response

31 A chirp signal is a harmonic signal which increases its frequency as a function of time.
In figures (7.45a & b), superimposed upon the LVDT frequency responses of the two channels, are adjusted mathematical models of the two channels. Firstly these frequency responses no longer represent system conversion of input(volts)-to-output(radians), rather input(volts)-to-output(LVDT volts) instead. The LVDT sensitivity gain (540(mV/mm)) was taken into account to convert from the LVDT voltage signal to LVDT measured position (in millimetres). Also it was necessary to adjust model electromechanical parameters for a better fit between mathematical model responses and LVDT responses. These altered parameters are listed in table (7.2). With these frequency responses, controllers can now be designed for both the azimuth and elevation channels, which is discussed in section 7.3.4. The final open loop transfer functions used for controller design and system modelling are given in equations (7.32a & b).

\[
\frac{\text{Out}(\text{mm})}{\ln(V)} = \frac{1.86 \times 10^5}{s^3 + 51.8s^2 + 1316.9s} \quad \ldots\ldots (7.32a) \text{LVDT estimate azimuth}
\]

\[
\frac{\text{Out}(\text{mm})}{\ln(V)} = \frac{1.77 \times 10^6}{s^3 + 198.46s^2 + 1.1 \times 10^4s} \quad \ldots\ldots (7.32b) \text{LVDT estimate elevation}
\]

From these transfer functions the gain and phase margins can be found. The gain and phases margins of each transfer function give a good indication of closed loop robustness and stability. The gain margin is defined as the change in open loop gain required to make the system unstable in closed loop. Systems with greater gain margins can withstand greater changes in system parameters before becoming unstable in closed loop. The phase margin is defined as the change in open loop phase shift required to make the closed loop system unstable. The phase margin is also a measure of the closed loop system tolerance to time delay [125, 2000][126, 1995]. The gain and phase margins for the azimuth and elevation transfer functions are given in table (7.3).

<table>
<thead>
<tr>
<th>Channel</th>
<th>Azimuth</th>
<th>Elevation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gain margin (dB)</td>
<td>7.37 at 5.77 Hz</td>
<td>1.81 at 16.7 Hz</td>
</tr>
<tr>
<td>Phase margin (degrees)</td>
<td>27.6 at 8.3 Hz</td>
<td>6.48 at 15.0 Hz</td>
</tr>
</tbody>
</table>

From the gain and phase margins of table (7.3) it is seen that neither the azimuth or elevation transfer functions, as they are, would be stable or robust in closed loop. However with the addition of a suitable controller the gain and phase margins of the loop transfer function can be improved. This is discussed in the next section.
7.3.4 Pointing Mechanism Feedback Control Demonstration.

This section describes the efforts undertaken in the use of the fibre optic concentrator position sensor to provide feedback control for the manufactured concentrator pointing mechanism. To this end the demonstration of closed-loop control is successful. However, due to difficulties in mechanism actuation, disturbance rejection is low and only marginally acceptable for the design requirements. However, an improvement in mechanism actuation is demonstrated, via simulations matched to experimental data, to significantly improve disturbance rejection.

A proportional controller and a proportional integral controller were designed for each axis of motion (azimuth and elevation). The design requirements for these controllers were based upon the monotone vibrational disturbance experienced on board the UK-DMC. The controllers were required to improve closed-loop stability and robustness at a bandwidth an order of magnitude above the UK-DMC monotone vibrational disturbance frequency of 0.6 Hz (therefore requiring a bandwidth of at least 6 Hz). For the azimuth axis of motion, the proportional controller was found to full-fill these design requirements, but the proportional integral controller was unable to achieve the required bandwidth frequency. For the elevation axis of motion, the proportional controller was found to be too unstable, whereas the proportional integral controller was able to full-fill the design requirements. In the following section the experimental verification of the azimuth proportional controller and the elevation proportional integral controller are discussed, along with the practical issues of implementing the controllers.

7.3.4.1 Control System Practical Issues

To demonstrate closed-loop control of the concentrator pointing mechanism an XPC-target control program was constructed in simulink, see figure (7.46).

![Figure 7.46: Simulink pointing system control program.](image-url)
In figure (7.46), we see the simulink block diagram model of the concentrator pointing control program. The block diagram "flow" is from left to right, starting with the block labelled "Analog Input" which accepts the photodiode signals indicative of which fibre in the bundle is being illuminated. The next block along ("Calibration") performs the calibration/normalisation procedure on the incoming fibre signals. The "ABCD" block interprets the calibrated fibre signals into the azimuth and elevation error signals measured in millimetres. The "ABCD" block then outputs the azimuth and elevation error signals, and a further signal which is the sum of the normalised fibre power in the A, B, C and D quadrants (referred to as the ABCD power signal). The block following is referred to as the "Saturation logic". This block is necessary to prevent the error signal for both the azimuth and elevation channels returning to zero as the spot moves off the fibre bundle tip. This effect was observed during sensor testing despite the division of the error signal by the ABCD power signal value. It is thought that this is due to the zero illumination bias voltage (7.5 mV) of the photodiodes. When the ABCD power signal value drops below a certain value, the block will saturate the error signal dependant on the current sign of the error signal. This process is shown graphically in figure (7.47).

![Graph showing error signal saturation.](image)

**Figure 7.47: Error signal saturation.**

The next block in the simulink control program is the block representing the actual controller. Just after this is the "dead band logic" block. Unfortunately it was found that the pointing mechanism suffered from a dead band effect, in that the electromechanical system is unresponsive to control signals below a certain magnitude. The "dead band logic" block attempts to compensate for this effect by increasing the control signal by an amount experimentally determined to be the smallest magnitude of control signal to which the mechanism will respond. However, it is thought that the size of the dead band varies over the range of motion of both the elevation and azimuth channels,
making it difficult to completely eliminate the observed dead band effect. Due to the dead band effect, the resulting sensitivity of the closed loop system will be reduced. The final block ("Analog output") sends the control signals, via the MD22 motor drive circuit, to the motors for both the azimuth and elevation control channels.

Closed-loop controller frequency response tests were conducted at the SSTL solar simulator facility. The equipment set up is shown in figure (7.48). In order to assess the closed-loop frequency response of the system it was necessary to simulate a disturbance in the system. This was done via adding band limited white noise to the azimuth or elevation channel, as shown in figure (7.46). In order to measure the sensitivity and complementary sensitivity functions for the closed-loop response, it was necessary to log error signal before and after the addition of the noise.
disturbance. By comparing this logged data with the logged band limited white noise, the sensitivity and complementary sensitivity functions can be determined, from which can also be derived the loop gain. During these closed loop frequency response tests, data was also logged on how motion along one axis of movement affect motion on the other axis of movement, a cross coupling effect between azimuth and elevation channels. This will be discussed in the following sections.

A simulink model of the entire system was also constructed and used for comparison with experimentally derived frequency response data. This also allowed for modelling the system with an approximated dead band effect introduced.

In the following sections model and experimental frequency response data is compared for two types of controllers.

7.3.4.2 Proportional Control of Pointing Mechanism

This section discusses the closed-loop performance of the azimuth channel under proportional control. From the mechanism characterisation it was found that a proportional gain of less than unity was required to increase phase and gain margins to improve closed-loop robustness and stability. The design point for the controller was that the system was required to be capable of attenuating disturbances at a frequency of 0.6 Hz, this being the observed angular disturbance frequency present on the UK-DMC. Therefore, the bandwidth of the loop transfer function was required to be at least an order of magnitude higher than this disturbance frequency. The proportional controller gain was subsequently identified to have a value of 0.2. (i.e. $C(s) = 0.2$)

In order to compare experimental data to a reference, a simulink model of the system was created, this is shown in figure (7.49).

![Simulink pointing system control model](image-url)
Figure (7.49) contains many of the same blocks as figure (7.46). There are, however, a few additional blocks. The block labelled “Bundle” simulates the response of the fibres and photodiodes to the position of the spot; as discussed in section 7.2.2. The other additional blocks are the open-loop transfer functions of the azimuth and elevation channels, which adequately represent the system. For clarity, the gain of 1.85 after the open-loop transfer function blocks for each channel in figure (7.49) is the linear conversion from LVDT voltage response (which is the output of the transfer functions in figure (7.49)) into concentrator spot location in millimetres.

Frequency response testing yielded the loop transfer function shown in figure (7.50) and the sensitivity and complementary sensitivity functions shown in figures (7.51 & 7.52).

![Figure 7.50: Azimuth proportional loop transfer function, experimental and simulated. (Please note that the phase plots for both the modeled cases are the same and lie on top of each other).](image)

![Figure 7.51: Azimuth proportional complementary sensitivity function, experimental and simulated.](image)
In Figures (7.50-7.52), the experimental closed-loop performance of the system on the azimuth channel is observed. Superimposed upon the experimental data is the simulated closed-loop performance of the system, via the simulink model in figure (7.49). Two situations were simulated via the simulink system model. In one case no dead band is present and in the other an amount of dead band is introduced based on estimation during system characterisation testing (± 150mV).

In figures (7.50-7.52), it is observed that there is good agreement between the dead band simulated performance and experimental simulated performance. Comparing these performances to the non-dead band simulated performance it can be seen that the presence of a dead band results in a decrease in sensitivity of the system.

However, as measured from figure (7.50), the phase and gain margin of the loop transfer function are much improved, over the original values (see table (7.3)), with the presence of a controller.

- Azimuth proportional controller: Gain margin = 9.8 dB (at ~6Hz).
- Azimuth proportional controller: Phase margin = 32° (at ~2.5Hz).

These values demonstrate an improvement in closed-loop stability and robustness. Please note that in figure (7.50) the phase of the modelled system for the cases of no dead band and dead band are the same.

In figure (7.51) the complimentary sensitivity function was a value of unity up until a frequency of ~5 Hz, indicative of the bandwidth frequency. This demonstrates that the system is capable of good signal tracking at almost an order of magnitude higher than the design point frequency of 0.6 Hz and is capable of tracking the SunAzimuth vector angular rate, identified in section 7.1.1.

In figure (7.52) the sensitivity function of the system has suffered from the dead band effect, and is only capable of providing a 7.5 dB attenuation of disturbances at 0.6 Hz. However, this can be improved with the removal of dead band from the system as observed for the non-dead band case for which a 11.2 dB disturbance attenuation is possible. This indicates that in order to achieve better disturbance attenuation, an improved, more sensitive mechanical system is required. Further more, this demonstrates that the fibre optic sensor is working well. The observed angular
disturbance for the UK-DMC has an amplitude of ~75 arcseconds at 0.6 Hz, which would result in a peak pointing error power loss of ~3%. With the azimuth control system, this disturbance could be reduced to ~30 arcseconds, resulting in a peak pointing error power loss of only ~0.25%.

Finally, it was of interest to observe the experimental cross coupling effect between the azimuth and elevation channel. This was observed by measuring the elevation channel error signal during azimuth channel frequency response testing. In figure (7.53) the level of cross coupling between the system azimuth and elevation channels are observed. In figure (7.53) it can be seen that the magnitude of any cross coupling is at approximately −30 dB over the frequency range of interest. This demonstrates that the sensor performs well at differentiating between the azimuth and elevation directions.

![Figure 7.53: Measured cross coupling between azimuth and elevation channels during azimuth proportional frequency response testing.](image)

In the next section control of the elevation channel is demonstrated via proportional integral control.

### 7.3.4.3 Proportional Integral Control of Pointing Mechanism

This section discusses the closed-loop performance of the elevation channel under proportional integral control. From the mechanism characterisation it was again found that a proportional gain of less than unity was required to increase the phase and gain margins in order to improve closed-loop robustness and stability. The design point for the controller was that the system was required to be capable of attenuating disturbances at a frequency of 0.6 Hz, this being the observed angular disturbance frequency present on the UK-DMC. Therefore, the bandwidth of the loop transfer function was required to be at least an order of magnitude higher than this disturbance frequency. The proportional integral controller identified to be suitable is given by equation (7.33):

\[
C(s) = \frac{0.45s + 5}{s} \quad (7.33)
\]

The proportional integral controller given in equation (7.33) was identified via manual controller tuning using the simulink system simulation (figure (7.49)). It was desired for this controller to exhibit similar gain and phase margins to the azimuth proportional controller but at a higher...
bandwidth (12Hz) than the azimuth proportional controller (5 Hz). In simulation this controller exhibited a gain margin of ~10 dB and a phase margin of ~20° with a system bandwidth of 12Hz. This was decided to be sufficient for experimental closed-loop testing, however, a more rigorous controller tuning procedure would likely produce a more stable and better performance system. Experimental frequency response testing yielded the loop transfer function shown in figure (7.54) and the sensitivity and complementary sensitivity functions shown in figures (7.55 & 7.56).

In figures (7.54-7.56), the experimental closed-loop performance of the system on the elevation channel is observed. Superimposed upon the experimental data is the simulated closed-loop performance of the system, via the simulink model in figure (7.49). Two situations were simulated via the simulink system model. In one case no dead band is present and in the other an amount of dead band is introduced based on estimation during system characterisation testing (± 500mV).

In figures (7.54-7.56), it is observed that there is better agreement between the dead band simulated performance and experimental simulated performance, compared to the agreement between the non dead band simulated performance. Comparing these performances to the non-dead band simulated performance, it can be seen that the presence of a dead band results in a significant decrease in sensitivity of the system. General agreement between simulated and experimental performance is not as good as seen for the azimuth proportional controller. This is most likely due to the elevation open loop transfer function model not being accurately representative. However, the typical behaviour of a proportional integral controller is clearly visible from the loop transfer functions in both modelled and experimental cases (figure (7.54)).

![Figure 7.54: Elevation proportional-integral loop transfer function, experimental and simulated. (Please note that the phase plots for both the modeled cases are the same and lie on top of each other).](image-url)
However, as measured from figure (7.54), the phase and gain margin of the loop transfer function are much improved, over the original values (see table (7.3)), with the presence of a controller.

- Elevation proportional integral controller: Gain margin = 7.7 dB (at ~18.7Hz).
- Elevation proportional integral controller: Phase margin = 50.3° (at ~4.3Hz).

It should be noted that the experimental phase margins exceed those predicted for the simulated cases, further open loop transfer characterisation testing will be necessary to obtain better agreement between experimental and simulated results. Please also note that in figure (7.55) the phase of the modelled system for the cases of no dead band and dead band are the same.

In figure (7.55) the complimentary sensitivity function was a value of approximately unity up until a frequency of ~10 Hz, indicative of the bandwidth frequency. This demonstrates that the system is capable of good signal tracking at an order of magnitude higher than the design point frequency of 0.6 Hz and should be capable of tracking the SunElevation vector angular rate, identified in section 7.1.1.
In figure (7.56) the sensitivity function of the system has suffered severely from the dead band effect, and is only capable of providing a 4 dB attenuation of disturbances at 0.6Hz. This is significantly different to the non-dead band case for which a 34.6 dB disturbance attenuation is possible. It is believed that the reason for the poor dead band compensation is due to the effect of gravity causing the dead band to be non-symmetric. This indicates that in order to achieve better disturbance attenuation, a much better mechanical system is required. The observed angular disturbance for the UK-DMC has an amplitude of ~75 arcseconds at 0.6Hz, which would result in a peak pointing error power loss of ~3%. With the elevation control system, this disturbance could be reduced to ~47 arcseconds, resulting in a peak pointing error power loss of ~1.0%.

It was of interest to observe the experimental cross coupling effect between the azimuth and elevation channel. This was observed by measuring the azimuth channel error signal during elevation channel frequency response testing. In figure (7.57) the level of cross coupling between the system azimuth and elevation channels are observed.

![Figure 7.57: Measured cross coupling between azimuth and elevation channels during elevation proportional-integral frequency response testing.](image)

In figure (7.57) it can be seen that the magnitude of any cross coupling is on average approximately -40 dB over the frequency range of interest. This demonstrates that the sensor performs well at differentiating between the azimuth and elevation directions. It should be noted that this observed cross coupling is slightly better than that for the azimuth proportional controller frequency response tests.

In order to demonstrate time-domain behaviour consistent with that observed in the frequency domain a Chirp signal frequency response test was performed. The results of which are plotted in figure (7.58).
In figure (7.58), the complementary sensitivity and sensitivity function are seen to exhibit the behaviour indicative of closed-loop control. For example, the amplitude of complementary sensitivity function decreases towards zero as the Chirp signal frequency increases and the amplitude of the sensitivity function increases towards the amplitude of the Chirp signal as the Chirp signal frequency increases. This is a further demonstration of the fibre optic sensor functioning well. This data was for a PI controller exhibiting a bandwidth of ~1 Hz. The pointing accuracy of the system is observed, from the data in figure (7.58) at low frequency, to be ± 0.05°, which is sufficient to fulfill the fibre to concentrator coupling efficiency requirement (5%) discussed in section 7.1.1.

Finally, a manual disturbance test was conducted to observe how the pointing system responded to relatively large angular shocks. Figure (7.59) shows the response of the elevation channel to a manual angular shock of 12500 μrad (~0.7°) causing the middle fibre in the bundle to be effectively displaced from the concentrator spot by ~1 mm. The top plot in figure (7.59) plots the middle fibre photodiode signal over the course of the test and also plots the sum of all seven fibre photodiode signals during the course of the test (ABCD power signal). This demonstrates that indeed the vast majority of power in the bundle is located on the central fibre (~85%). The bottom plot depicts the azimuth and elevation positions during the course of the test.
From figure (7.59) it is clear that the current pointing system is capable of recovering from angular shocks much larger than those reported for the UK-DMC and can maintain very accurate pointing to ensure efficient transmission of solar energy. However, the time taken to recover from the shock takes longer than indicated by the bandwidth (~10 Hz) of the frequency responses shown in figures (7.54-4.56). This indicates that the frequency response performance is non-linear with disturbance amplitude.

### 7.3.4.4 Pointing Mechanism Feedback Control Demonstration Conclusion

The fibre optic concentrator position sensor was successfully employed to demonstrate closed-loop control of the two axes of revolution (azimuth and elevation) of the two axis parabolic concentrator pointing control mechanism, driven by two DC brush motors. Controllers were designed for each axis of revolution, based upon open-loop system identification tests and the design requirements of having improved robustness and stability at a bandwidth at least an order of magnitude above the vibrational angular disturbance frequency of 0.6 Hz, observed to be present on the UK-DMC. For the azimuth axis of revolution, a proportional controller was found to full-fill these design requirements and comparison between modelled and simulated frequency response tests showed good agreement. For the elevation axis of revolution, a proportional integral controller was found to ful-fill these design requirements, however, comparison between modelled and simulated frequency response did not agree as well as the azimuth case, indicating inadequate system identification. However, in both the azimuth and elevation cases, it was shown...
that the presence of dead-band in the mechanism gearing resulted in a significant reduction in system sensitivity. For each case it was shown by simulation that system sensitivity could be improved with the removal of the dead-band. It is clear that in order to experimentally demonstrate improved pointing system sensitivity, improved mechanism actuation is required. However, the fibre optic concentrator position sensor functioned well and the system was able to demonstrate a pointing accuracy of 0.05°, which is sufficient to full-fill the concentrator to fibre coupling efficiency, identified in section 7.1.1.

7.4 Chapter Summary and Conclusions

7.4.1 Chapter Summary

In this chapter the development of a closed-loop concentrator pointing control system has been described. Initially, the need for a closed-loop pointing control system is established and the theory of space mechanism actuators and classical control theory were reviewed. Then, a novel fibre optic sensor was introduced, capable of providing direct feedback control of a concentrator pointing control system. Two sensor position algorithms were discussed and the efforts to manufacture a working fibre optic sensor described. With the manufacture of a concentrator pointing mechanism, open-loop frequency response testing was conducted and modelled results were matched to experimental data. Finally, closed-loop concentrator pointing control was demonstrated for two types of controller and frequency response data was matched to modelled results.

7.4.2 Chapter Conclusions

From the analysis and discussion in this chapter the following conclusions were made:

- In order to achieve the minimum required overall system efficiency of 60%, a concentrator to fibre coupling efficiency of 95% is required. This requires a concentrator pointing mechanism to point the concentrator at the Sun with an accuracy of ~2 arc minutes.

- The use of multiple small concentrators reduces the power required to orientate the concentrator system. There is significant reduction of system moment of inertia in using multiple small concentrators in comparison to a single large concentrator.

- A gaussian intensity profile results in concentrator to fibre coupling efficiency being less sensitive to the small separations between the fibre and concentrator spot, in comparison
to a square concentrator spot intensity profile. Measured data indicates that the spot for the 105mm diameter concentrator, in solar simulator laboratory conditions, has a gaussian intensity distribution with a $\sigma$ value of $-0.25$.

- Exact pointing algorithm is more accurate than the ABCD pointing algorithm, but it is difficult to implement and is computationally intensive.

- Comparison of fibre power in the concentrator pointing control fibre bundle, in the manner that is done for the ABCD pointing algorithm, is representative of the Cartesian position of the concentrator spot on the fibre bundle tip.

- Fibre luminance intensity as measured by a photodiode is linearly related to fibre transmission power as expected. This was confirmed up to a fibre transmission power of 4.5 W.

- Careful manufacture of the concentrator pointing control fibre bundle is required to ensure no cross coupling and allow consistent fibre calibration values for each fibre in the bundle.

- Experimental evaluation of the behaviour of the concentrator spot position signal from the concentrator pointing control fibre bundle sensor, demonstrated the sensor's behaviour was consistent with simulated behaviour, indicating that the assumptions made for simulated behaviour were sufficient to understand the real sensor.

- Controllers employed to demonstrate concentrator pointing feedback control, improved closed-loop stability and robustness. This satisfied the design requirement of being capable of attenuating disturbances at a frequency in order of magnitude higher than the observed vibrational disturbance observed onboard the UK-DMC micro-satellite (0.6Hz).

- Good agreement between azimuth experimentally measured frequency response data and simulated frequency response data was observed, indicating that the mathematical model of the azimuth channel, derived from system characterisation, is sufficiently representative.

- Marginal agreement between elevation experimentally measured frequency response data and simulated frequency response data was observed, indicating that the mathematical model of the elevation channel, derived from system characterisation, is not truly representative of the system, although similar responses were observed. Further characterisation is required for the elevation channel.
• Dead band frequency response simulation data was more in agreement with experimentally measured frequency response data for both the azimuth and elevation channels. This demonstrates that the presence of dead band in the system results in a decrease in system sensitivity.

• The fibre optic concentrator position sensor functioned well and the system was able to demonstrate a pointing accuracy of 0.05°, which is sufficient to full-fill the concentrator to fibre coupling efficiency.
Chapter 8

8 Summary and Conclusions

8.1 Overview

This chapter summarises the research conducted at the University of Surrey into the application of fibre optics, capable of the transmission of high intensity solar radiation, to solar thermal propulsion system technology for small-satellites. It discusses the highlights of the research, including contributions to solar thermal propulsion system design and mission analysis, to the analysis of coupled conductive and radiative heat transfer in participating media and to novel sensor technology for closed-loop control of solar concentrators. This chapter will also make specific recommendations for further research.

8.2 Summary

The purpose of this research was to investigate the utility and feasibility of a fibre optic augmented solar thermal propulsion system for a small-satellite. The research has shown that such a propulsion system is indeed feasible and would offer more system versatility over conventional solar thermal propulsion concepts for small satellites, while still offering a competitive performance. For this research a series of critical system components were investigated, and three critical system components were specifically manufactured for testing. These included:

- A small parabolic concentrator mirror suitable for coupling light to a 1mm core size fibre optic.
- A fibre optic heat exchanger representative of a direct-gain solar receiver.
- A small concentrator pointing control mechanism with a novel fibre optic pointing determination sensor.

Testing of these components was compared to simulated model results for which good agreement was observed.

The advantages of applying fibre optics to a small-satellite solar thermal propulsion system were highlighted: these include:
• Mechanical decoupling of the solar receiver and solar concentrator: This allows the concentrator and receiver to be freely located on the spacecraft, providing design versatility and the ability for direct-gain receivers to thrust in a direction other than along the Sun vector (for fixed concentrators).

• Use of multiple concentrators: This allows for more system versatility and can provide a means of significantly reducing the concentrator system mass, which is desirable for small spacecraft.

• Use of multiple thrusters: Through optical switching of concentrator power, multiple receivers can be employed and also different types of receiver, offering a wider variety of thruster strategies.

Analysis of two types of small-satellite missions demonstrate the utility of a fibre optic augmented solar thermal propulsion system for a small-satellite. These missions consider the use of solar thermal propulsion in Low Earth Orbit, which may have significant periods of eclipse, meaning that the power source to heat the propellant is not constant. However, in this analysis it is shown that with the use of fibre optics and thermal-storage receiver technology, an STP system can be designed to overcome this disadvantage to provide competitive performances.

• DMC inspector: An inspector satellite making regular transfers between satellites of DMC constellation, 1025m/s ΔV requirement. The main benefit for this mission is the ability to use a direct-gain thrust strategy, via the fibre optic augmented STP system, which is more appropriate for Low Earth Orbit manoeuvres as it allows faster receiver heat up time.

• High Latitude Earth Observation small-satellite constellation: A fibre augmented solar thermal propulsion system acts as a transfer vehicle for a small satellite from LEO to GTO, 2440m/s ΔV requirement. The main benefit for this mission is the use of fibre optics to heat a series of thermal-storage receivers, which are scaled in terms of size and deliverable thrust. The ability to switch between different receivers at different times during the manoeuvre allows for a reduction in trip time over a single thruster, while maintaining system performance.

In view of how fibre optics would benefit small-satellite solar thermal propulsion systems, a series of system critical components were selected for further analysis. The first of these components was a solar concentrator suitable for efficiently coupling solar energy into a fibre optic. Initially, fixed, rigid, parabolic concentrators were identified to be most appropriate for a small-satellite STP system. The benefits of fibre augmentation were theoretically demonstrated via the decrease in concentrator system mass by using multiple small concentrators. Appropriate design
parameters were identified to manufacture a concentrator specifically for a given fibre optic cable. The analysis in particular found that in order to effectively couple light from a parabolic dish concentrator to a fibre optic cable, the rim angle of the concentrator should be the same as the acceptance angle of the fibre optic. Detailed modelling of a concentrator, using a raytrace model constructed in matlab, was shown to provide results consistent with a ZEMAX (professional optical design software package) model of the same concentrator. This matlab model was then used to determine concentrator surface quality requirements and it found that a surface RMS form error of <0.1 μm was necessary for acceptable concentration. With the selection of a suitable fibre optic cable (Polymicro FLU) a concentrator was designed and manufactured. A series of experimental tests were conducted which demonstrated:

- Measurement of surface form error: The manufactured concentrator was found to possess an acceptable surface form error of 0.08 μm.
- Concentrator efficiency: When used in a Cassegrain style optical system an overall efficiency of 83% was exhibited.

Further tests demonstrated that the concentrator intensity distribution appeared to be consistent with modelled results.

Consideration was given to determine a suitable fibre optic cable. Initially, the desirable properties of a suitable fibre were identified and the causes of possible intrinsic and environmental fibre attenuation were reviewed. A selection of candidate fibres was identified and the FLU fibre optic of Polymicro was selected for practical testing due to its high numerical aperture (NA=0.66), allowing for high concentration ratios and subsequently high obtainable receiver temperatures. In the process of considering the thermal behaviour of the FLU fibre optic cable, a novel leapfrog algorithm is described which is concerned with coupled conduction and radiation heat transfer problems. This algorithm was shown to be more accurate, stable and less computationally demanding than current solution methods for a given desired accuracy. This algorithm was employed to estimate the temperature profile of the FLU fibre optic subjected to heating while transmitting concentrated solar energy and it was shown that the fibre would remain below the manufactures maximum temperature specification over a two hour heating period. The FLU fibre optic cable was subjected to transmission and thermal testing. Fibre power transmission levels were observed to be as high as ~4.5 W, demonstrating a 50% overall transmission efficiency. Fibre optic thermal tests demonstrated that significant attenuation resulted from the fibre temperature exceeding the manufactures maximum temperature limit and stressed the need for strict thermal control of the FLU fibre optic. The observed increase in attenuation was attributed to the degradation of the cladding and buffer material at high temperatures.
Consideration was given to determine the performance of ammonia direct-gain STP systems, a system analysis tool was constructed based on an existing model for thermal-storage STP systems. The analysis tool was used to determine the performance of an ammonia direct-gain fibre augmented STP system for the UK-DMC micro-satellite as a function of available small concentrator mirrors. It was found that for best-case component efficiency, seven small concentrators would enable a performance of 300s specific impulse to be achieved. To investigate effective fibre optic heating, a heat exchanger representative of a small molybdenum direct-gain receiver was constructed for vacuum heating testing. Initial heating tests resulted in low receiver temperatures, due to poor absorption of the concentrated solar energy. However, absorption was improved via coating the interior of the receiver with high temperature black paint and better still with graphite. A peak temperature of the molybdenum receiver, heated effectively from a single concentrator (power wise), was observed to be $\sim$550 K.

The final critical component considered was the concentrator pointing system. Analysis of pointing error demonstrated that arc-minute pointing accuracies were necessary to provide an acceptably efficient coupling between the small concentrator mirror and FLU fibre optic. The pointing error was also shown to have a significant impact on system specific impulse. During FLU fibre transmission testing, the fibre was observed to exhibit a luminescence when transmitting high intensity light; this was attributed to fibre photoluminescence, Rayleigh scattering and non-perfect total internal reflection. It was found that the intensity of the fibre luminescence had a proportional relationship with the power the fibre was transmitting, as measured by a photodiode sensor. It was then proposed that the measuring of the relative luminescence of fibres in a bundle would allow the calculation of pointing error, thereby providing a sensor by which closed-loop pointing control could be enabled. This novel concept was further developed by the development of pointing error algorithms and then by the manufacture and practical testing of a working sensor. The sensor was shown to exhibit behaviour consistent with modelled results and was subsequently used to demonstrate closed-loop control of a two-axis concentrator pointing control mechanism.

8.3 Objectives and Conclusions

At the start of this research project a series of research objectives were established to provide a clear path of research direction. The extent to which these objectives have been accomplished and the conclusions concerning these objectives are now discussed:

- Objective: Identification of significant benefits brought to a small spacecraft STP system through the introduction of fibre optics in terms of:
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- Orbital manoeuvring strategies.
- System versatility and integration.
- System redundancy.

This objective was accomplished via researching previous system concepts from the literature, concept realisation, concept visualisation and first order modelling of system concepts. The significant system benefits brought to a small spacecraft STP system through the introduction of fibre optics are primarily related to enhancing system versatility, conformity to small spacecraft constraints and system redundancy, rather than providing improved system performance. See chapter 3 section 3.4 for specific conclusions.

- Benefits in orbital manoeuvring strategies: vary depending upon the type of STP system being considered. The STP system which benefits the most from the application of fibre optics is a direct-gain STP system which has a fixed/static concentrator, as fibre optics allows the mechanical decoupling of the receiver from the concentrator, so that a larger range of thrusting vectors can be provided for such a STP system.

- For other STP systems the application of fibre optics mainly benefits system versatility (for example optical switching allows different types of STP receiver to be employed), system integration (for example reduction in concentrator system mass by employing multiple small concentrators in place of a single large concentrator) and system redundancy (multiple small concentrators and multiple receivers).

- A first order design and mass break down based on conventional micro-satellite propulsion system technology, demonstrated that it is feasible to accommodate a STP system onboard a micro-satellite, with the system still being capable of exceeding the performance of conventional micro-satellite propulsion systems.

- Objective: Identification of small-spacecraft high ΔV mission concepts, which could gain or become feasible through the application of a solar thermal propulsion system augmented with fibre optics.

Two small-spacecraft high ΔV mission concepts were identified to potentially benefit through the application of a solar thermal propulsion system augmented with fibre optics. These were investigated in detail via computational modelling (using Satellite Tool Kit). In comparison to
other small-spacecraft high ΔV STP mission concepts identified in the literature; these two concepts dealt with low Earth orbit manoeuvres where a large proportion of the orbit is in eclipse. From the analysis of these two mission concepts it was concluded that a fibre optic augmented STP system could be feasibly used for efficient low Earth orbital manoeuvring. See chapter 3 section 3.4 for specific conclusions.

- Objective: Investigation of significant system components and how the integration of fibre optics affects the design considerations. System components include:
  - Solar concentrator
  - Fibre optics
  - Solar receiver
  - Solar concentrator pointing system

Investigations into significant system component designs were based upon the UK-DMC ammonia direct-gain STP system concept.

- Solar concentrator: The investigation concerning the solar concentrator was limited to considering coupling between a concentrator and a single fibre optic rather than a bundle of fibre optics. It was found that the major design characteristics of a parabolic dish concentrator are significantly dependent on the optical properties of the fibre optic to which it is coupled. Coupling of a concentrator to a single fibre dictates the use of small concentrators, as fibre optic diameters are at the most ~1 mm in size. Furthermore, the rim angle of a parabolic concentrator must be consistent with the numerical aperture of the
Fibre optic, to ensure efficiency coupling. For more detailed conclusions see chapter 4 section 4.5.2.

- **Fibre optics**: The investigation concerning fibre optics for STP focussed on identifying causes of fibre attenuation in a space environment and suitable fibre optics for the application. It was concluded that fused silica core fibre optics offer the most appropriate attenuation properties for this application. In order to achieve high receiver temperatures, requires highly concentrated solar energy, it was therefore concluded that very large numerical aperture (NA = 0.66) fibre optics were required. On investigating the thermal behaviour of fused silica fibre optics, it was concluded that the FLU fibre optic of Polymicro would be able to survive 2 hours of continuous heating, without requiring external cooling, before the peak fibre temperature exceeded the manufacturers limit of \(-460\) K. For more detailed conclusions see chapter 5 section 5.4.2.

- **Solar receiver**: The investigation concerning the solar receiver focussed on deriving propulsive efficiency of an ammonia direct-gain STP system and considered fibre to receiver coupling. With the application of fibre optics to a STP system there is a decrease in overall system efficiency (-20%). The desired peak receiver temperature for high efficiency propulsive performance is significantly higher than the maximum exposure temperature of the selected fibre optic. It was concluded that a means of radiatively coupling the fibre to the receiver was required such that the fibre remained below its maximum exposure temperature. Thermal modelling of a concept that allows direct radiative interaction between the fibre and the receiver and only indirect conductive interaction, demonstrated that this is possible. For more detailed conclusions see chapter 6 section 6.4.2.

- **Solar concentrator pointing system**: The investigation concerning the solar concentrator pointing system focussed primarily on the development of the fibre optic concentration pointing determination sensor, which is made possible by the application of fibre optics to the STP system and allows for direct closed-loop pointing control of the concentrator to provide efficient coupling between the fibre and concentrator. For more detailed conclusions see chapter 7 section 7.4.2.

- **Objective**: Derive system component requirements and overall system efficiency for an example fibre augmented STP system, through system component modelling.
System component modelling was conducted for all significant components and the following requirements were derived:

- **Solar concentrator**: Mathematical modelling of a concentrator suitable for coupling light to a 1mm diameter fibre optic with a numerical aperture of 0.66 indicated that the concentrator was required to have a diameter of 105 mm and a rim angle of ~40°. Ray-trace optical modelling of the 105 mm parabolic dish concentrator concluded that the concentrator required a surface quality of ~0.1 μm in order to achieve the concentrator peak concentration ratio of 11,100. Required system efficiency 90% derived.

- **Fibre optics**: Mathematical modelling of a fibre optic transmitting concentrated solar energy demonstrated that higher numerical aperture fibres would result in higher receiver peak temperatures. Computational thermal modelling of the FLU fused silica fibre optic indicated that the fibre would be heated, via absorption of the transmitted solar energy, to a temperature of ~440 K over a 2 hour duration heating period. From the literature a fibre transmission efficiency of ~74% was expected.

- **Solar receiver**: Computational modelling of the UK-DMC ammonia direct-gain fibre augmented STP system required an overall system efficiency of 60% in order to produce a propulsive performance of 300s with a 6, 105 mm diameter concentrator array. A receiver absorption efficiency of 95% was concluded to be required.

- **Solar concentrator pointing system**: Computational modelling of concentrator to fibre coupling indicated that the concentrator pointing system must point the concentrator towards the Sun with an angular accuracy of 2 arc-minutes to obtain a concentrator to fibre coupling efficiency of 95%.

- **Objective**: Experimentally investigate feasibility of obtaining component requirements.

System components were purchased and manufactured for experimental feasibility testing:

- **Solar concentrator**: The 105 mm diameter small parabolic concentrator was manufactured to have a surface quality of ~0.08μm. Maximum concentration ratio obtained in laboratory conditions ~5,000, unable to improve due to lack of light collimation from the solar source employed. Efficiency testing of the concentrator demonstrated an efficiency...
of 83%. It was concluded that achieving concentrator component requirements was feasible.

- **Fibre optics:** Fibre optic transmission and attenuation spectrum testing indicates the presence of extra attenuation processes associated with the transmission of highly concentrated solar energy. An overall concentrator with fibre efficiency of ~50% was practically demonstrated. Extreme temperature fibre exposure tests emphasise the requirement to maintain the peak fibre temperature below the manufacturers limit of ~460K.

- **Solar receiver:** Graphite sample fibre optic heating demonstrated an overall system efficiency of ~46% with an absorption efficiency of ~92%. Vacuum heating tests of a fibre optic heat exchanger representative of the UK-DMC ammonia direct-gain STP demonstrator receiver demonstrated improved absorption efficiency when coating the interior of the receiver with graphite. Absorption efficiency was 54% for a cavity with a bare molybdenum surface. This was increased to 82% by coating the interior of the receiver with graphite, however the peak receiver temperature was limited due to design and experimental inefficiencies.

- **Solar concentrator pointing system:** Closed-loop concentrator pointing control tests demonstrated that the fibre optic concentration pointing determination sensor was capable of pointing the concentrator at the Sun with an accuracy of 3 arc-minutes.

From the analysis of the significant component testing results it was concluded that it is feasible for these system components to achieve the imposed requirements.

- **Objective:** Design, manufacture and test prototype system.

Although individual system component prototypes were designed, manufactured and tested, these prototype components were not tested all as one. Furthermore, no experimental propulsion testing was conducted in this research. However, the results from individual system component testing provided significant evidence to demonstrate that such a system is feasible and could provide efficiency propulsive performance.
8.4 Recommendations for Further Research

The comprehensive nature of the research conducted required the pursuit of multiple avenues of investigation that could benefit from further study. These include:

- Investigation into the numerical stability of the Leapfrog algorithm for coupled conductive and radiative heat transfer problems. In the examples studied the Leapfrog algorithm was observed to be stable at time steps equivalent to a single jump to steady state values, when other algorithms were unstable. However, it was not proven that the Leapfrog algorithm is unconditionally stable for these types of heat transfer problems. This should be investigated further.

- Experimental verification of the behaviour of silica fibre optics attenuation when exposed to a radiation environment consistent with Low Earth Orbit and Mid Earth Orbit. The expected behaviour was inferred from the literature, but could not be verified by experiment.

- Additional vacuum heating tests for the fibre optic heat exchanger. With improved vacuum conditions and additional solar concentrators, a higher receiver temperature could be achieved and confirmed experimentally. In doing so, the thermal protection of the FLU fibre optic can be further evaluated. Furthermore, with the manufacture of an actual direct-gain receiver, propulsive performance could be demonstrated.

- Thermal testing of an all-silica fibre optic. All-silica fibre optic cables now exist, which have numerical apertures approaching that of the FLU fibres. These fibres are preferable to the FLU fibre in terms of their resilience to high temperature. It is, however, recommended that such fibres undergo similar thermal testing as the FLU fibre in this research.

- In order to demonstrate an improved closed-loop performance of the concentrator pointing control mechanism, a more sensitive mechanism would be desirable.

- The optical switching method proposed in this research should be investigated further to ascertain the concentrator coupling efficiency. This would require the manufacture of a larger fibre optic bundle and also a more elaborate pointing algorithm and control system.

- A concentrator pointing Sun acquisition method is needed. This can either be done via open-loop pointing of the concentrator, or by employing a concentrator search algorithm which makes use of the fibre optic concentrator pointing control sensor.
9 References


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10 Appendix A: Alternative Pointing System Algorithms

In this appendix the other pointing algorithms considered for the interpretation of fibre optic sensor signals in to Cartesian coordinate telemetry of the position of the concentrator spot on the tip of the fibre optic sensor are derived and discussed.

10.1 Six-Fibre Square Spot Intensity Profile Pointing Algorithm

The original concept of the fibre optic sensor for closed-loop pointing control of a parabolic concentrator pointing mechanism was not to employ photodiodes sensors to measure fibre luminescence, rather to use photodiodes to measure the transmission of light through the fibres surrounding the central fibre in the bundle. The surrounding fibres were assumed to be highly attenuating and could reduce the intensity of the transmitted solar energy to be measurable by a photodiode (so the intensity of the transmitted energy in the surrounding fibres did not cause damage to the photodiode or cause the photodiode signal to saturate). This meant that only the surrounding fibres could be employed to gather pointing control telemetry, as the central fibre would be the fibre used to deliver solar energy to the solar thermal receiver.

10.1.1 Pointing System Problem Definition

![Figure A.1: Fibre bundle tip with concentrator spot.](image-url)
We consider a concentrator solar image (spot) trained on the end of a fibre optic cable. The spot is the same dimensions as the fibre optic, and we need to control the concentrator pointing so that the spot is aligned with the fibre.

To achieve this we surround the fibre by six others and measure the light flux in all six surrounding fibres (see figure (A.1)). The light intensity is just proportional to the area of overlap with the spot, and from this we can determine the centre of the spot.

![Diagram of overlapping circles](image)

**Figure A.2: Overlapping circles.**

We note that if the centre of the spot moves beyond twice the fibre radius \(2r\) from the centre we can observe the same fluxes in the surrounding fibres as when it is inside. Hence to determine the position of the spot we need to assume it lies within \(2r\) of the central fibre, or monitor rate of changes of flux.

Consider two circles which intersect, both having the same radius \(r\). They intersect at two points (M and N). Since OM=PM, the triangle ΔOMP is isosceles. MN is then the bisector of the angle θ OMP, and therefore intersects OP perpendicularly at the point L. From figure (A.2) the separation \(s\) between the two circles is given by:

\[
OP = s = 2r \cos \theta \quad \text{(A.1)}
\]

We wish to calculate the area of overlap of the two circles.

The area of the segment OMT = \((1/2) r^2 \theta\).

The area of the triangle ΔOML = \((1/2) r^2 \sin \theta \cos \theta\).

Therefore the area MLT = \((1/2) r^2 \theta - (1/2) r^2 \sin \theta \cos \theta = (1/2) r^2 (\theta - \sin \theta \cos \theta)\)

By symmetry the area of overlap \(A\) is four times the area of MLT.

\[
A = r^2 (2\theta - \sin 2\theta) \quad \text{(A.2)}
\]

It should be noted here that if \(s\) is equal to \(2r\) then \(A\) is equal to zero. Furthermore, if \(s\) is equal to zero then \(A\) is equal to \(\pi r^2\), which is consistent with the circles lying on top of one another.
10.1.2 Intersection with Two Circles

Suppose the spot straddles two fibres centred on O&M. Let the position of the centre of the spot, from O, the centre of the main fibre, be \((s, \phi)\) (see figure (A.3)).

The area of overlap between O and P is given above. To find the area of overlap between P and M we need the distance PM\(=s'\). From the triangle \(\Delta OMP\):

\[
s'^2 = s^2 + (2r)^2 - 2s(2r) \cos \phi
\]

and \(s = 2r \cos \theta\), \(s' = 2r \cos \theta'\)

therefore \(4r^2 \cos^2 \theta' = 4r^2 \cos^2 \theta + 4r^2 - 8r^2 \cos \theta \cos \phi\)

therefore \(\cos^2 \theta' = \cos^2 \theta + 1 - 2 \cos \theta \cos \phi = \cos^2 \theta + \cos^2 \phi + \sin^2 \phi - 2 \cos \theta \cos \phi\)

therefore \(\cos^2 \theta' = \cos^2 \theta + \sin^2 \phi \cos^2 \phi\) ..........(A.3)

This determines the angle \(\theta'\), hence the area of overlap of M and P is:

\[
A' = r^2 (2 \theta' - \sin 2 \theta') \ldots . (A.4)
\]

However, can we be sure that we always get a solution? The circles P and M will just touch when \(s' = 2r\).

If \(\cos \theta' = 1 \Rightarrow 1 = (\cos \theta - \cos \phi)^2 + \sin^2 \phi\)

therefore \(\cos^2 \phi = (\cos \theta - \cos \phi)^2\)

implying \(\cos \phi = \cos \theta - \cos \phi\) or \(\cos \phi = \cos \phi - \cos \theta\)

implying \(\cos \phi = (1/2) \cos \theta\) or \(\cos \theta = 0\)
The second of these solutions means O and P are just touching. Hence \( s = s' = 2r \), so the triangle \( \triangle OMP \) is an equilateral triangle. Hence \( \varphi = 60^\circ \) and the spot lies exactly over the next surrounding fibre.

This also proves that we can pack 6 surrounding fibres, since \( \varphi = 60^\circ \) and so for one revolution is 6 fibres will fit around the central fibre just touching. So if the spot makes an angle \( \theta \) with the central fibre then there are in general three surrounding fibres which satisfy the condition \( \cos \varphi \leq 1/2 \cos \theta \). For each of these fibres we can then compute \( \theta' \) and hence \( A \).

### 10.1.3 Solving the Inverse Problem

If we know the radiative flux on each of the surrounding fibres can we locate the centre of the spot? For this we need to invert the previous problem.

![Figure A.4: Two overlapping circles.](image)

The general problem will have non-zero responses in 3 neighbouring fibres: \( C_0, C_1, C_2 \). We have chosen \( C_0 \) to be the middle of the three. \( C_2 \) lies clockwise from \( C_0 \) and \( C_1 \) lies anticlockwise (see figure (A.4)).

Due to regular spacing, \( \angle C_1OC_0 = \angle C_0OC_1 = \pi/3 \), we can use \( OC_0 \) as the direction \( \varphi = 0 \). We wish to find \((s, \varphi)\) of \( P \) where \( s = OP \) and \( \varphi = \angle POC_0 \).

From the fluxes we know the areas \( A_0, A_1, A_2 \), of overlap between the spot \( P \) and each of \( C_0, C_1, C_2 \) respectively.

For \( C_0 \):

\[
A_0 = r^2 (2\theta_0 - \sin 2\theta_0)
\]

where

\[
\cos^2 \theta_0 = (\cos \theta + \sin \varphi)^2 + \sin^2 \varphi = \cos^2 \theta + 1 - 2 \cos \theta \cos \varphi \quad \text{...(A.5)}
\]

and \( s = 2r \cos \theta \). Hence \( A_0 \) determines \( \theta_0 \) and this gives an expression for \( \theta \) and \( \varphi \).
For $C_2$:

$$A_2 = r^2 (2\theta_2 - \sin 2\theta_2)$$

where

$$\cos^2 \theta_2 = \cos^2 \theta + 1 - 2 \cos \theta \cos (\pi/3 - \varphi) \ldots \ldots (A.6)$$

Subtracting equation (A.6) from equation (A.5) gives:

$$\cos^2 \theta_0 - \cos^2 \theta_2 = -2 \cos \theta [\cos \varphi - \cos (\pi/3 - \varphi)]$$

and

$$\cos (\pi/3 - \varphi) - \cos \varphi = \cos (\pi/3) \cos \varphi + \sin (\pi/3) \sin \varphi - \cos \varphi$$

but

$$\cos (\pi/3) = 1/2 \Rightarrow 2 \cos (\pi/3) = 1$$

therefore

$$\cos (\pi/3 - \varphi) - \cos \varphi = \cos (\pi/3) \cos \varphi$$

therefore

$$\cos (\pi/3 - \varphi) - \cos \varphi = -\cos (\pi/3 + \varphi)$$

therefore

$$\cos^2 \theta_0 - \cos^2 \theta_2 = -2 \cos \theta (\cos (\pi/3 + \varphi))$$

For $C_1$:

$$A_1 = r^2 (2\theta_1 - \sin 2\theta_1)$$

and

$$\cos^2 \theta_1 = \cos^2 \theta + 1 - 2 \cos \theta \cos (\pi/3 + \varphi) = \cos^2 \theta + 1 + \cos^2 \theta_0 - \cos^2 \theta_2$$

therefore

$$\cos \theta = \cos^2 \theta_1 + \cos^2 \theta_2 + \cos^2 \theta_0 - 1$$

implying

$$s^2 = (2r)^2 (\cos^2 \theta_1 + \cos^2 \theta_2 - \cos^2 \theta_0 - 1) \ldots \ldots (A.7)$$

and

$$2 \cos \theta \cos \varphi = 1 + \cos^2 \theta - \cos^2 \theta_0$$

therefore

$$\cos \varphi = \frac{\cos^2 \theta_1 + \cos^2 \theta_2 - 2 \cos^2 \theta_0}{2 \cos \theta} \ldots \ldots (A.8)$$

Equations (A.7 and A.8) therefore solve the inverse problem. It should be noted that as the spot settles on the central fibre, (as $\theta$ approaches $\pi/2$) the denominator in equation (A.8) vanishes. This means we cannot determine $\varphi$ with any accuracy, so a controller must concentrate on $s$ and drive $s$ to zero.

The accuracy of this method, for the six-fibre sensor problem, is the same as shown in figures (7.16 & 7.17).
10.2 Locus Method Pointing Algorithm

This method is similar to that above in that it uses the photodiode signals to estimate the distance the spot is away from each fibre. However, this algorithm allows for seven or six fibres and also for larger spots with a distribution of intensity. In the same way as the algorithm discussed in section 7.2.1 a look up table of the assumed photodiode response to the spot intensity distribution is employed to estimate the distance the centre of the spot is away from the fibre in question, see figure (A.5).

Figure A.5 Pointing software correlation comparison

When the distance of the spot center from each fibre is estimated a series of circles can be drawn around each fibre the radius of which is equal to the estimated distance that the spot center is away from a particular fibre. Then the spot centre position is estimated by calculating the position of all the intersection points of all the fibre circles via a locus technique. The average position of these circle intersection points is the estimate of the spot centre position. This approach, although computationally demanding, allows tracking to be accomplished with any size of focal spot, provided the intensity distribution is known, and also allows for photodiode signals to be present for all fibres at the same time providing algorithm robustness. A model of this method was created.
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In Matlab to assess its accuracy. Averaging the coordinates of these intersections estimates the spot location. Figure (A.6) depicts this locus technique in practice for two spot displacements:

![Figure A.6: (a) Position estimation for 0mm offset. (b) Position estimation for 0.5mm offset.](image)

In figure (A.6) the red spot is the estimated position of the spot centre and the red circle is the actual spot. Position error increase with increased distance from the centre, however the vector of motion is always correct. Figure (A.7) depicts the increase in estimate position error with increasing radial distance of the bundle from the centre of the spot.

![Figure A.7: Position estimation error vs. radial distance of spot centre from central fibre](image)
11 Appendix B: Practical Issues of an STP system on a Small-Spacecraft

In this appendix the basic calculations of receiver heating are reviewed and the practical issues of locating a hot receiver on a small-spacecraft are discussed.

11.1 Receiver Heating Calculations

As an example of basic receiver heating calculations the three thermal-storage receivers of the HLEO mini-satellite STP concept are sized to demonstrate this. The basic equations for receiver heating are:

\[ \eta P_j = \frac{1}{2} T l_{sp} \] ......(B.1)

\[ P_h = M_r C_p \frac{\Delta T}{\Delta t} \] ......(B.2)

where \( \eta \) is system efficiency, \( P_j \) is jet power (power into the propellant), \( P_h \) is heating power, \( T \) is required thrust, \( l_{sp} \) is required specific impulse, \( M_r \) is receiver mass \( C_p \) is receiver specific heat capacity \( \Delta T \) is change in receiver temperature and \( \Delta t \) is time. The requirements of the three HLEO thermal-storage receivers are listed in table (B.1):

<table>
<thead>
<tr>
<th>Receiver</th>
<th>Required ( l_{sp} ) (s)</th>
<th>Heating time (s)</th>
<th>Manoeuvre duration (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2-N</td>
<td>2</td>
<td>(&lt;3,000)</td>
<td>1,200</td>
</tr>
<tr>
<td>6-N</td>
<td>6</td>
<td>(&lt;9,000)</td>
<td>1,200</td>
</tr>
<tr>
<td>12-N</td>
<td>12</td>
<td>(&lt;18,000)</td>
<td>1,200</td>
</tr>
</tbody>
</table>

The properties of the three HLEO thermal-storage receivers are listed in table (B.2):
Table B.2: HLEO thermal-storage receiver requirements

<table>
<thead>
<tr>
<th>Receiver</th>
<th>Specific heat capacity (J kg⁻¹ K⁻¹)</th>
<th>Efficiency</th>
<th>Jet Power (W)</th>
<th>Receiver Mass (kg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2-N</td>
<td>2,500</td>
<td>1</td>
<td>3,924</td>
<td>1.26</td>
</tr>
<tr>
<td>6-N</td>
<td>2,500</td>
<td>1</td>
<td>11,772</td>
<td>3.77</td>
</tr>
<tr>
<td>12-N</td>
<td>2,500</td>
<td>1</td>
<td>23,544</td>
<td>7.53</td>
</tr>
</tbody>
</table>

Where jet power is calculated from equation (B.1) for each receiver and receiver mass is calculated from equation (B.2) by equating jet power to heating power and assuming a manoeuvre duration of 1,200s and a change in receiver temperature from 2,500 K to 1,000 K. To estimate the times required to heat up each of the thermal storage receivers to 2,500K equation (B.2) is used, where the heating power is calculated from the solar power collected from a 1.5 m diameter concentrator consistent with the dimensions of a mini-satellite. The heating times for these receivers are given in table (B.3):

Table B.3: HLEO thermal-storage receiver requirements

<table>
<thead>
<tr>
<th>Receiver</th>
<th>Heating Power (W)</th>
<th>Heating time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2-N</td>
<td>2,400</td>
<td>1.969</td>
</tr>
<tr>
<td>6-N</td>
<td>2,400</td>
<td>5.891</td>
</tr>
<tr>
<td>12-N</td>
<td>2,400</td>
<td>11.766</td>
</tr>
</tbody>
</table>

The heating times stated in table (B.3) are for maximum efficiency heating and do not take into account radiative heat loss. As the receiver is heating up it will slowly approach its steady state temperature. When it reaches steady state temperature the radiative heat loss from the outside surface of the insulation will equal the concentrator input power. For a more detailed look at receiver heating times it is necessary to employ the Kennedy thermal-storage receiver modelling tool. Figures (B.1 – B.3) show the temperature vs. time receiver heating models for each of the HLEO thermal storage receivers. For each of the thermal-storage receivers the receiver is heated to a temperature of 2,500 K and then allowed to over the same duration as the heating time.
Figure B.1: (a) 2-N receiver heat-up and cooling temperature vs. time simulation. (b) 6-N receiver heat-up and cooling temperature vs. time simulation (c) 12-N receiver heat-up and cooling temperature vs. time simulation.
From the temperature vs. time plots of receiver heat-up and cooling shown for each receiver on figure (B.1), it can be seen that obtaining a receiver temperature of 2,500 K within the required heating times is possible. It is also observed that radiative cooling of each receiver reduces the temperature of each receiver below the minimum firing temperature of 1,000 K within a time on the same order as that taken to heat the receiver up to the maximum temperature of 2,500 K.

11.2 Practical Concerns of a Fibre Optic Augmented STP System

The purpose of this section is to discuss the practical concerns of a fibre optic augmented STP system onboard a small-spacecraft in light of what has been discussed earlier in this document. For example, one of the main practical concerns to be addressed is what effect will the exposure of the spacecraft to the extreme temperatures of the solar receiver have on the spacecraft.

A solar receiver for an STP system onboard a spacecraft would reach temperatures as high as 2,500 K. As the receiver is required to be attached to the spacecraft, in order to provide thrust, there must be a conductive heat transfer link between the receiver and the spacecraft. This means that there will be conductive heat loss from the receiver into the spacecraft. There will be payload instruments and electronic avionics on board the spacecraft, which will have strict temperature regulations imposed on them. It is therefore necessary to place the receiver as far away as possible from these instruments to ensure the safety of the spacecraft mission and platform. To reduce the heat lost from the receiver to the spacecraft platform it will be necessary to insulate the spacecraft platform with a material with a low thermal conductivity (see chapter 6 for discussion on insulation materials). A low thermal conductivity will mean that the time taken for heat to leak into the spacecraft platform will be reduced. The cross sectional area of the insulation structure supporting the receiver should be minimised as this will limit the heat transfer rate between the receiver and spacecraft platform. This relationship is seen mathematically in equation (B.3):

\[ Q_x = -kA \frac{dT}{dx} \quad \text{(B.3)} \]

where \( Q_x \) is the heat transfer rate, \( k \) is the thermal conductivity of the insulation material, \( A \) is the cross sectional area of the insulation structure and \( \frac{dT}{dx} \) is the temperature gradient between the receiver and the spacecraft platform. In critical areas the spacecraft platform can be cooled via radiating fins in order to maintain temperature.

The practical concerns of fibre optics are primarily related with hot fibre compatibility with electrical wiring within the spacecraft and the size of fibre bend radius that can be achieved. Fibre optic bend radii are often quoted by the manufacturer to be \(-300\) times the core fibre radius. The FLU fibre was subjected to a 180° bend during fibre optic transmission testing at a bend radius of
30 mm. During these tests no decrease in attenuation was observed, however, the fibre did exhibit a large amount of elastic energy. Therefore to subject these fibres to such a tight bend radius requires a supporting structure.

It was shown earlier in this work that fused silica fibre optics transmitting concentrated sunlight will experience significant heating over time. It is of concern as to what effect exposure of the hot fibre will have to spacecraft instruments and electrical components. The portion of the fibre experiencing the most heating is the portion closest to the concentrator system. If it is necessary to cool the fibre optic cables before they come into contact with any electrical wiring, this can be done away from the concentrator system and accomplishing with radiating fins or passing the fibres through a heat sink to dissipate the heat transfer along the fibre. The rate of heat transfer along the fibre will be moderately slow due to fused silica’s low thermal conductivity of 1.71 Wm⁻¹K⁻¹.
12 Appendix C: Concentrator Technical Drawing

On the next page the technical drawing of the small parabolic dish concentrator specially designed for coupling light to the FLU fibre optic cable is shown. This technical drawing was sent to Precision Optical Engineering Ltd for manufacture.