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Design of a wide-temperature-range star camera and analysis of thermally induced aberrations and stray light

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Abstract: As a critical component for spacecraft attitude measurement, inertial reference establishment, and rendezvous and docking missions, the stringent requirement for sub-arcsecond-level pointing accuracy in star cameras demands a compact optical mechanism and an opto-mechanical-thermal (OMT) integrated design. This paper presents a star camera based on a coaxial quad-reflector optical configuration. The primary and tertiary mirrors share a common substrate, achieving an effective aperture of 64 mm and a focal length of 195 mm. The four aspheric mirrors provide a $\pm 2.5^\circ$ field of view with an objective numerical aperture of 0.26. The system exhibits outstanding optical performance, with an MTF exceeding $0.7@40$ lp/mm, approaching the diffraction limit. 80% encircled energy diameter $\leq 11 \mu\text{m}$, absolute distortion not exceeding 0.1%. The system employs Zerodur[®] glass ceramics and 4J32 Invar alloy, which coupled with a flangeless primary mirror and a compact 50 mm axial layout, ensure excellent thermal insensitivity. Coupled finite element analysis, using 36 Zernike polynomials, shows that within a temperature range of $-40^\circ\text{C}\sim 60^\circ\text{C}$, the root mean square (RMS) surface error of the primary mirror remains below 6.19 nm. Rigid-body displacement is maintained within $\pm 0.5 \mu\text{m}/10 \text{ K}$, MTF degradation remains below 20%, and the fluctuation of the 80% encircled energy diameter is less than $5 \mu\text{m}$. Stray light suppression is achieved using a lightweight baffle ring with a multi-stage extinction cone. The extended baffle yields a Point source transmittance of 3×10^{-9} at 30° off-axis, improving stray light suppression by two orders of magnitude and enabling detection of faint 10th-magnitude stars. Thermal vacuum testing validated the system's performance, demonstrating a centroid positioning repeatability of 0.08 pixels and providing a valuable reference for the design of high-precision space optical payloads.

Key words: star camera; opto-mechanical-thermal integration analysis; zernike polynomial fitting; point source transmittance

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宽温域星相机设计及热致像差与杂散光分析技术

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摘要:星相机作为航天器姿态测量、惯性基准建立与交会对接等任务中的关键部件, 其亚角秒级高精度指向对光机结构紧凑设计与多物理场耦合分析提出严格要求。本文提出一种采用同轴四反光学结构的星相机, 主镜与三镜采用一体化设计, 系统有效口径 64 mm, 焦距 195 mm, 借助四片非球面反射镜实现 $\pm 2.5^\circ$ 视场, 物方数值孔径 0.26。系统光学性能优异, $\text{MTF} > 0.7@40 \text{ lp/mm}$, 接近衍射极限。80% 能量集中分布于 $11 \mu\text{m}$ 内, 绝对畸变不超过 0.1%。结构上使用 Zerodur[®]微晶玻璃与 4J32 因瓦合金组合, 结合无凸台主镜与轴向 50 mm 紧凑布局, 实现优良的热不敏感特性。采用有限元分析提取 36 项泽尼克系数进行拟合的耦合计算方式, 分析得到在 -40°C 至 60°C 范围内, 主镜面形均方根误差变化小于 6.19 nm , 刚体位移控制在 $\pm 0.5 \mu\text{m}/10 \text{ K}$, MTF 衰减低于 20%, 80% 能量圆直径波动小于 $5 \mu\text{m}$ 。杂散光抑制采用带多级消光锥的轻量化遮光环, 长遮光罩使点源透射比在 30° 离轴条件下达 3×10^{-9} , 杂散光抑制能力提升两个数量级, 满足 10 等星暗弱目标探测需求。系统经热真空试验验证, 质心定位重复性为 0.08 个像素, 为高精度航天光学载荷设计提供了重要参考。

关键词:星相机; 光机热耦合分析; 泽尼克多项式拟合; 点源透射比

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1 Introduction

As a core component of spacecraft attitude control systems^[1], star trackers primarily determine the three-dimensional orientation of a spacecraft by observing the positions of stars within their coordinate systems and matching them against pre-stored star maps^[2]. Star cameras operate on the same measurement principle as star trackers, but typically feature larger relative apertures, enabling the detection of fainter stars and other dim space objects^[3]. Consequently, they are regarded as a distinct category of optical payloads. In recent years, the rapid advancement of high-resolution Earth observation and deep-space exploration missions has imposed increasingly stringent requirements on the imaging quality and positioning accuracy of star cameras^[4].

The star camera experiences significant environmental changes throughout its lifecycle, from

ground commissioning and launch to in-orbit operation^[5]. Particularly during the in-orbit phase, it is exposed to external thermal fluxes such as the cold, dark space environment and periodic solar thermal radiation. Optical payloads can withstand a wide temperature range of $-60^\circ\text{C} \sim 70^\circ\text{C}$. However, rapid and uneven temperature variations can induce thermally induced structural deformation in the camera's optical system^[6-7]. Specifically, changes in mirror surface shape and thermal displacement of components may degrade imaging quality and cause optical axis drift^[8], ultimately reducing the pointing accuracy of the camera. Therefore, the thermal stability of the star tracker's optical system is a core factor in ensuring its measurement accuracy under complex space conditions. During the development of planetary cameras, OMT integrated simulation serves as an indispensable analytical tool for iterative optimization of optical and structural designs to achieve thermal stability targets, as well as for fea-

ibility assessments of established design concepts. This simulation method interrelates multiple physical fields such as OMT. It quantitatively illustrates the influence of temperature-gradient-induced structural deformation on optical performance through optical evaluation metrics^[9]. A commonly used method for OMT integrated simulation involves first importing the opto-mechanical model of the star camera into software such as ANSYS for thermal and structural analysis. The resulting mirror deformation data are then fitted using Zernike polynomials. Subsequently, rigid-body displacement data, variations in material refractive index, and the fitted Zernike coefficients obtained from thermal-stress analysis are re-imported into the original optical design for optical performance evaluation^[10]. In practical applications, Liu Yongqing *et al.* employed OMT integrated simulation using finite element analysis and Zernike polynomial fitting to analyze a large-aperture thermally insensitive star sensor design. The accuracy of this integrated analysis was validated through actual assembly and adjustment tests^[11]. However, a critical technological challenge remains, how to extract Zernike coefficients from finite element analysis and establish an accurate and efficient multiphysics data transfer and characterization mechanism. This would enable the representation of complex structural deformations induced by thermomechanical loads as optical aberrations recognizable and assessable by the optical system, ultimately achieving precise prediction and active control of degradation patterns in optical performance.

In addition to the influence of the thermal environment, stray light represents an unavoidable interference factor in space optical systems and significantly affects the imaging quality and positioning accuracy of star trackers^[12-13]. Stray light refers to unwanted light rays that enter the optical system through reflection, scattering, or diffraction, primarily originating from the Sun, Earth's atmosphere, the Moon, or other space radiation sources^[14-15]. Such

stray light degrades image contrast and signal-to-noise ratio, potentially overwhelming faint target signals, leading to failed celestial body identification, or increasing attitude calculation errors. Suppressing stray light has thus become a critical challenge for improving system performance in high-precision deep-space exploration missions. Simulating stray light in star cameras is an essential method for optimizing optical configurations and baffle designs. Software such as LightTools or Ansys Speos can simulate stray light propagation paths, energy distribution, and the effectiveness of suppression mechanisms within the system. By quantifying metrics such as point source transmittance (PST). Simulations guide the optimization of design parameters, including baffle layout, aperture placement, and surface coatings^[16]. These simulations not only allow prediction of in-orbit stray light effects during ground-based development to prevent post-launch performance degradation, but also help reduce research and development costs and risks, ensuring that the satellite cameras maintain high-resolution observation capabilities under extreme space optical environments. In practical applications, Ryota Fuse *et al.* performed ray-tracing-based stray light analysis on the wide-angle multispectral camera of the Mars Moon eXploration (MMX) spacecraft. Their work estimated the ratio of stray light intensity to target signal intensity, thereby guiding the preliminary camera design^[17]. Shi Chunlei *et al.* established an optical-mechanical structural model of a long-wave infrared reflective system in the stray light analysis software TracePro. They calculated the PST curve of the system to simulate and evaluate whether the baffle design met requirements, thereby informing decisions on whether system cooling was necessary^[18].

This paper focuses on the structural and optical performance analysis of a star camera, with an emphasis on the optical stability and stray light suppression capabilities enabled by the structure design. The aim is to provide a reference for the design and

feasibility evaluation of high-precision aerospace optical payloads. The remainder of this paper is organized as follows: Chapter 2 presents the optical design results of the coaxial quadruple-reflection camera and its corresponding opto-mechanical structure. Chapter 3 focuses on the thermal stability simulation of the system and evaluates the effect of temperature on optical properties. Chapter 4 discusses system stray light simulation to evaluate whether the optical configuration, mechanical structure, and baffle design can achieve the required stray light rejection ratio.

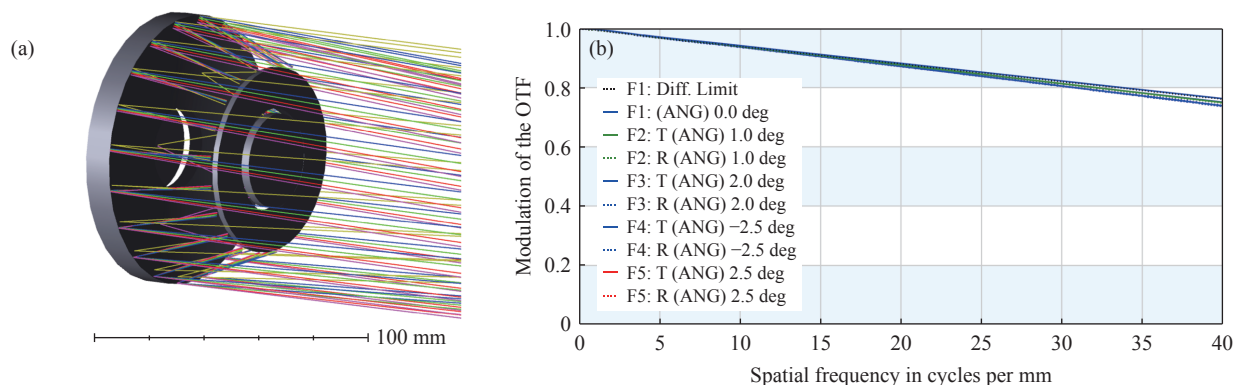
2 Coaxial quadruple-reflection star camera design

This system employs a coaxial quadruple-reflection design based on the Cassegrain configuration, achieving aberration optimization by introducing third and fourth mirrors in addition to the primary and secondary mirrors. The optical parameters are summarized in Table 1. The system features an effective aperture of 64 mm, a focal length of 195 mm, a circular field of view of $\pm 2.5^\circ$, and an object-side numerical aperture (NA) of 0.26. Following optimization, the primary and tertiary mirrors were integrated into a monolithic design, enabling the construction of the coaxial quadruple-reflector structure using only three aspheric mirrors. The three-dimensional optical layout is illustrated in Fig.1(a). Parallel light is first reflected by the primary mirror onto the secondary mirror, then re-directed to the tertiary mirror, and finally passes

through the fourth mirror within the secondary mirror aperture to reach the image plane. At an effective focal length (EFL) of 195 mm, the system achieves outstanding optical performance. As shown in Fig.1(b), the optical transfer function (OTF) approaches the diffraction limit, with $\text{MTF} \geq 0.7@40 \text{ lp/mm}$. There is no significant difference between the meridional and sagittal transfer functions, ensuring excellent imaging quality even in the peripheral field. Fig.1(c) presents the point spread function (PSF) of the system. The main lobe width of the PSF is smaller than the pixel size. As shown in Fig. 1(d), 80% of the EE(Encircled Energy) is concentrated within an $11 \mu\text{m}$ diameter. This enables efficient energy concentration over a 2×2 pixel detector area, thereby directly enhancing the image signal-to-noise ratio (SNR) and the system's capability for detecting faint stars.

Tab. 1 Optical parameters of the star camera

Parameters	Design Specifications
Effective focal length	195 mm
Field of view	$\pm 2.5^\circ$
Operating Waveband	400-1000nm
Absolute Distortion	$\leq 0.1\%$
Energy concentration (within $11 \mu\text{m}$)	$\geq 80\%$
Transmission function (40 lp/mm)	≥ 0.7
Operating Temperature	$-40 \sim 60^\circ\text{C}$
Equivalent entrance pupil diameter	64 mm
Axial length	50 mm
Pixel size	$6.5 \mu\text{m}$



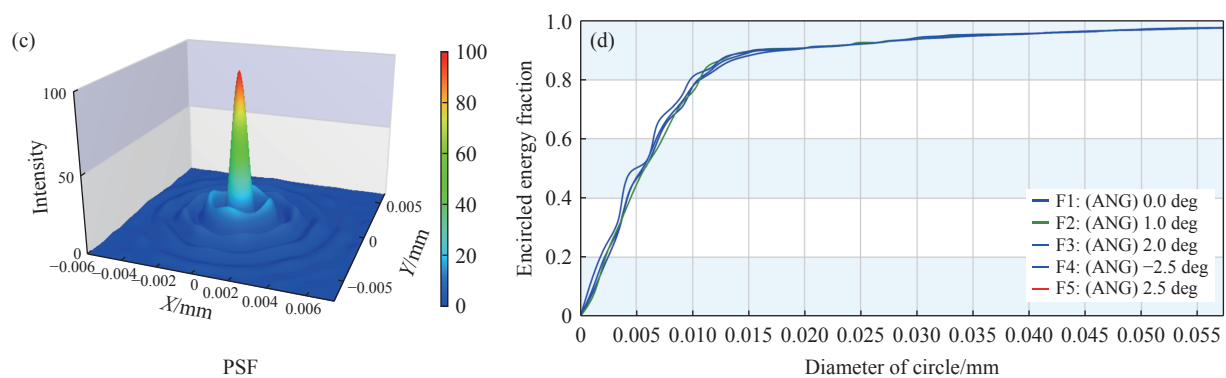


Fig. 1 Optical design of coaxial quadruple-reflection star camera. (a) Schematic 3D model of the camera. (b) MTF diagram at 22°C. (c) PSF diagram at 22°C. (d) Diffraction ring energy distribution diagram at 22°C.

The operating temperature range spans $-40^{\circ}\text{C}\sim 60^{\circ}\text{C}$ with no vignetting. The equivalent entrance pupil diameter is 64 mm, and the system axial length is controlled at 50 mm.

The specific structural parameters are listed in Table 2. The primary mirror has a diameter of 102 mm, a radius of curvature of -219.2 mm, and a conic constant of 3.92. The secondary mirror measures 68 mm in diameter, with a radius of curvature of -190.66 mm and a conic constant of 1.85. The tertiary mirror is integrated with the primary mirror and shares the same parameters. The fourth mirror has a diameter of 35 mm, a radius of curvature of -240 mm, and a conic constant of 5.91. The precise spacing between each element optimizes the obscuration ratio to 0.67. This design achieves a light-weight configuration. The fully reflective structure completely eliminates chromatic aberration, and with a dedicated silver coating, it delivers consistent imaging performance across the target operating wavelength range.

Tab. 2 Parameters of the star camera optical structure

Surface	Diameter (mm)	Radius (mm)	Cone coefficient	Interval from the previous surface(mm)	obscuration ratio(%)
Primary Mirror (Integrated with the third mirror)	102	-219.2	3.92	0	67%
Secondary Mirror	68	-190.66	1.85	-38	-
Fourth Mirror	35	-240	5.91	-41	-

The analysis of this study is based on the as-

sumption of ideal alignment. However, if optical tolerances are excessively stringent, making high-precision assembly unfeasible in practice, then regardless of how excellent the thermal stability of the optical system appears in simulation, it will fail to achieve the expected performance after actual assembly. Tolerance allocation is shown in Table 3. Validated through cumulative error analysis and Monte Carlo simulation, it not only demonstrates the system's excellent manufacturability but also provides alignment guidance.

Tab. 3 Star Camera Tolerance Allocation Table

Surface	Alignment Tilt (")	Alignment Radial Tolerance (mm)	Alignment Axial Deviation (mm)	Radius Error (mm)	Surface Figure Error (wave)	Remarks
Primary Mirror	-	-	-	0.05	0.033	Primary mirror as reference
Secondary Mirror	10	0.005	0.005	0.05	0.033	
Fourth Mirror	10	0.005	0.005	0.05	0.033	

3 Integrated design and analysis of optical-mechanical-thermal system

The cross-sectional view of the optical-mechanical structure is shown in Fig.2(a). To ensure thermal stability, the mirror substrates are fabricated from Zerodur® glass ceramic, which has an extremely low linear thermal expansion coefficient. All mirrors employ silver-coated films, which typically exhibit outstanding reflectivity across the vis-

ible to near-infrared spectrum. Modern silver reflective films incorporate a multilayer composite structure comprising bonding and protective layers on their surfaces. Utilizing advanced techniques such as magnetron sputtering, these films significantly enhance coating adhesion and provide effective isolation against environmental corrosion. This advancement significantly improves the environmental adaptability of silver films, enabling them to maintain stable reflective performance in practical applications^[19]. Combining high reflectivity across a broad wavelength range with the superior environmental stability conferred by process improvements, silver films represent the optimal choice for the long-term in-orbit operational conditions of this star camera.

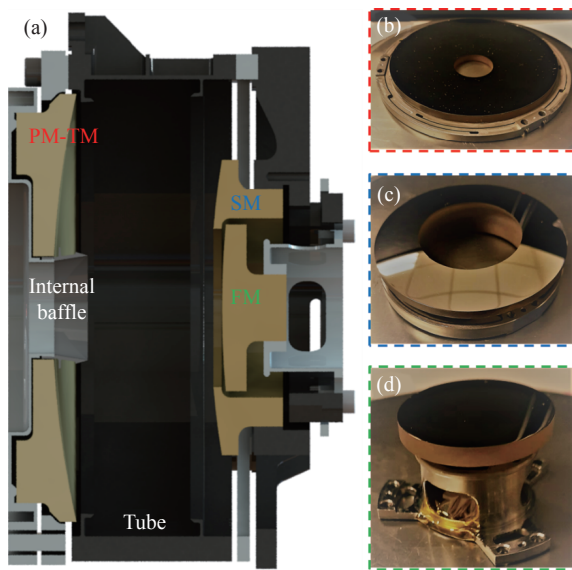


Fig. 2 (a) structural profile. (b) primary three-mirror assembly. (c) secondary mirror assembly. (d) four-mirror assembly

All structural components are made of Invar alloy, whose thermal expansion coefficient is matched to that of the Zerodur[®] glass ceramic. This material pairing minimizes variations in mirror surface figure and rigid-body displacement during temperature cycling. The primary function of the load-bearing cylinder is to serve as the main support and maintain the separation between the primary and secondary mirrors. It incorporates reinforcing ribs to en-

hance rigidity while minimizing mass. The inner baffle is constructed from 6061 aluminum alloy and features an internally mounted configuration around the primary mirror. Its main function is to block stray light from entering the optical system, thereby reducing external optical interference. Since the primary mirror has a concave surface whose reflective properties are sensitive to light direction, the internal baffle prevents stray light from being directly reflected into the observation path. The primary mirror is rigidly supported and fixed at the rear end of the tube, minimizing deformation while maintaining vibration resistance, forming a stable assembly with the internal baffle. The secondary mirror is suspended in front of the primary mirror via a spider-type support frame. Its highly complex structure increases stray light contributions, but the secondary mirror and fourth mirror employ an internally nested design, significantly reducing the overall system envelope. Fig.2(b)-(d) respectively illustrate the opto-mechanical components of the primary-third mirror (PM-TM), secondary mirror (SM), and fourth mirror (FM). The PM-TM is the largest optical element in the system. Consequently, its structural stability is a primary factor influencing overall camera performance. Employing a flangeless design and utilizing Invar alloy with matched thermal expansion coefficients throughout the structure, the primary mirror incorporates flexible directly mounted outermost periphery for stress isolation. This achieves a high degree of integration while meeting requirements for lightweight construction and compact axial length.

Due to the use of aspheric surfaces and the integrated PM-TM structure, the drawbacks of high functional integration become pronounced. With fewer optical elements to distribute error sensitivities, the burden of aberration correction is concentrated on individual mirrors, amplifying the impact of surface error and positional misalignments. As a result, the system exhibits significantly increased sensitivity to environmental disturbances. From an

optical design perspective, the system's relatively high object-side NA of 0.26 is positively correlated with increased optical sensitivity^[20]. Given the stringent stability requirements of such systems, an accurate OMT integrated simulation is essential to ensure system robustness. This approach ultimately enables an optimal balance between demanding performance specifications and the engineering constraints of miniaturization.

To complete the OMT integrated design, as shown in Fig.3(a), the geometric model of the multi-component assembly of the star camera was first imported into Altair HyperMesh to establish a finite element model. The Geometry module was used for cleanup and simplification to ensure surface continuity and eliminate unexpected geometric errors, thereby improving mesh quality and simulation accuracy. Subsequently, the Mesh module was employed to generate the mesh using a hybrid meshing strategy. For the complex curved surfaces of the optical components, tetrahedral elements (C3D10) were used to accurately capture high-gradient thermal fields. For structurally simple components, hexahedral elements (C3D8R) were adopted to enhance computational efficiency. The final mesh comprised 14,536 elements and 24,818 nodes, all generated using solid elements. The finite element mesh model of the optical system is shown in Fig.3(b). Boundary conditions simulating the thermal environment were defined in the Loads and Constraints module.

Steady-state temperature loads were applied across the extreme temperature range required by the simulation task: -40°C to $+60^{\circ}\text{C}$. For mechanical boundary conditions, fixed constraints were applied to the base flange to represent a rigid connection with the in-orbit satellite interface. The prepared model was then exported to the OptiStruct solver, incorporating temperature-dependent material properties as listed in Table 4. Post-processing in HyperView displayed the simulation results. The optical system employs Zerodur[®] glass ceramic for

the mirrors. Its high specific stiffness provides a foundation for lightweight structural design and effectively suppresses mirror deformation due to self-weight. The mechanical support structure utilizes Invar alloy, which helps maintain optical system stability across a wide temperature range. Fig.3(c)-(d) and Fig.3(e)-(f) present displacement contour plots for operating conditions at -40°C and 60°C , respectively, with displacement results visually magnified to clearly illustrate deformation patterns. Analysis indicates zero displacement in the constrained mounting region, while differences in thermal expansion between optical elements and support structures induce axial stresses. The three-point flexure support structure effectively accommodates these stresses along the optical axis. The system exhibits a maximum displacement of $0.65\text{ }\mu\text{m}$ at -40°C , which decreases to $0.43\text{ }\mu\text{m}$ at 60°C .

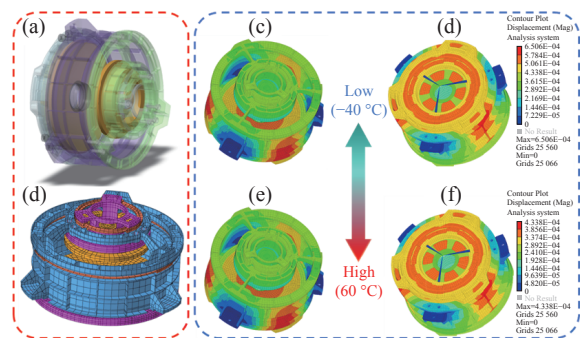


Fig. 3 (a) 3D rendered perspective view of the system. (b) Finite element meshed model diagram. (c) Solid displacement contour map at -40°C for the forward incident direction. (d) Solid displacement contour map at -40°C for the reverse direction. (e) Solid displacement contour map at 60°C for the forward incident direction. (f) Solid displacement contour map at 60°C for the reverse direction.

Tab. 4 Material properties of the machine structure				
component	Material	Density (g/cm ³)	Modulus of elasticity (GPa)	Coefficient of thermal expansion (10 ⁻⁶ K ⁻¹)
Mirrors	Zerodur [®]	2.53	91	0.05
structural components	Invar alloy	8.9	141	1.4

Despite the compact overall dimensions of the system, frame deformation still induces significant displacement responses in the optical elements. Not-

ably, while maximum displacement occurs at the edges of the optical elements, it primarily manifests as rigid-body rotation about the optical axis rather than axial deformation. The flexible support structure effectively converts thermal expansion into controllable micro-rotation, validating the design's excellent thermal management performance.

Accurately characterizing surface deformation induced by thermal-mechanical loading is a critical step in evaluating the aberration performance of a system. Zernike polynomials, as a classical set of orthogonal basis functions, are widely used for fitting optical surface deviations and decomposing aberrations. They provide optical design researchers with an intuitive mathematical interpretation of system quality, enabling subsequent performance optimization^[21]. The core principle involves projecting complex three-dimensional surface displacement fields onto the Zernike polynomial space. By solving for coefficients via least squares or Fourier transforms, this approach achieves a low-dimensional parametric representation of surface topography. Such fitting efficiently captures low-order aberration modes that dominate optical performance variations while neglecting high-frequency errors. The transformation from displacement variations to curvature variations can be expressed using the matrix equation (1).

$$\mathbf{Z} \times \mathbf{L} = \mathbf{C}_S, \quad (1)$$

In the equation, $\mathbf{Z}$ denotes the $p \times q$ matrix of the Zernike polynomials, p represents the number of sampling nodes, q indicates the number of polynomial terms, $\mathbf{L}$ is the column vector of Zernike coefficients, and $\mathbf{C}_S$ describes the surface profile variation of the measured optical surface. From the perspective of polar coordinates, the expression for the Zernike polynomials in polar coordinates can be written as:

$$Z_n^l(\rho, \theta) = R_n^l(\rho) \cdot \varphi_n^l(\theta), \quad (2)$$

Here, $Z_n^l(\rho, \theta)$ denotes the polynomial value at normalized radial distance ρ ($0 \leq \rho \leq 1$) and azimuth

angle θ ($\theta \in [0, 2\pi]$). n is a non-negative integer representing the radial degree, while l is the azimuth degree. n and l must share the same parity, with $n \geq |l|$, ensuring that $n - l$ is even. $R_n^l(\rho)$ is the radial polynomial, and $\varphi_n^l(\theta)$ is the azimuth function. By introducing $l = n - 2m$ ($m = 0, 1, 2, \dots, n$), the radial part can be expressed as

$$R_n^l(\rho) = R_n^{n-2m}(\rho) = \begin{cases} \sum_{b=0}^m (-1)^b \frac{(n-b)!}{b!(m-b)!(n-m-b)!} \cdot \rho^b, & n-2m > 0 \\ R_n^{n-2m}, & n-2m \leq 0 \end{cases}, \quad (3)$$

The forward summation is derived based on Jacobian polynomials, ensuring a polynomial form of degree n , while $-l$ employs symmetry mirroring for forward correspondence. The orientation component is defined as

$$\varphi_n^l(\theta) = \varphi_n^{n-2m}(\theta) = \begin{cases} \cos[(n-2m)\theta], & n-2m \geq 0 \\ -\sin[(n-2m)\theta], & n-2m < 0 \end{cases}, \quad (4)$$

This radial-angular separation structure facilitates modeling the optical properties of a circular aperture. An arbitrary wavefront $F(\rho, \theta)$ can be approximated by a finite-order expansion as

$$F(\rho, \theta) = \sum_{k=0}^{\infty} a_k Z_n^{n-2m}(\rho, \theta), \quad (5)$$

Where a_k represents the coefficient, typically determined using least squares or orthogonal projection methods to quantify the contribution of each aberration and support adaptive optical correction^[22]. This paper employs an amplitude normalization scheme, setting the maximum amplitude of each term on the unit circle to 1 for intuitive interpretation of peak-to-valley deviations. Priority is given to lower-order terms, progressing from global aberrations such as translation, tilt, and defocus to higher-order aberrations. The polynomial order must be selected based on system symmetry or application requirements to minimize truncation error. This paper employs the first 36 terms of the Zernike standard polynomial for fitting.

To convert the aforementioned finite element temperature-mechanical coupling node radial and axial displacements, along with mirror surface profile changes, into optical design inputs, a structure-optics interface method was employed to perform Zernike polynomial fitting of the surface topography. This primarily involved importing the perturbation file output from the finite element simulation and the system's finite element model, selecting a set of nodes of interest, and generating an optical design-compatible macro file. During the fitting stage, the polynomial type is set to standard Zernike polynomials with maximum order $n=10$ and $m=7$ to ensure all coefficients for the first 36 terms of the standard Zernike polynomials are fitted. Dur-

ing optical analysis, appropriate parameters are configured to ensure the standard Zernike polynomials are sorted using Noll ordering and the correct macro file is output. As shown in Table 5, the above algorithm principles were used to calculate the rigid body displacement of the primary-tertiary, secondary, and quaternary mirrors at 20°C intervals within the -40°C to 60°C temperature range. Data analysis indicates the PM-TM has root mean square (RMS) values of 6.19 nm and 4.12 nm at -40°C and 60°C, respectively, PV of 25.4 nm and 16.9 nm, respectively. The SM exhibited RMS values of 1.47 nm and 0.977 nm, with PV of 5.09 nm and 3.39 nm. The FM recorded RMS values of 0.114 nm and 0.076 nm, with PV of 0.61 nm and 0.41 nm.

Tab. 5 Rigid body displacement data

Component	Temperature (°C)	Tx (mm)	Ty (mm)	Tz (mm)	Rx (mm)	Ry (mm)	-Rz (mm)	ΔRoC (mm)	RMS (mm)	P-V (mm)
PM-TM	-40	5.06E-10	2.99E-10	-5.42E-04	4.09E-12	-8.73E-13	-4.85E-07	-3.93E-03	6.19E-06	2.54E-05
PM-TM	-20	3.37E-10	1.99E-10	-3.61E-04	2.73E-12	-6.23E-13	-3.23E-07	-2.62E-03	4.12E-06	1.69E-05
PM-TM	0	1.69E-10	9.96E-11	-1.81E-04	1.31E-12	-2.85E-13	-1.62E-07	-1.31E-03	2.06E-06	8.45E-06
PM-TM	40	-1.69E-10	-9.96E-11	1.81E-04	-1.31E-12	2.85E-13	1.62E-07	1.31E-03	2.06E-06	8.45E-06
PM-TM	60	-3.37E-10	-1.99E-10	3.61E-04	-2.73E-12	6.23E-13	3.23E-07	2.62E-03	4.12E-06	1.69E-05
SM	-40	-1.43E-10	-1.61E-11	2.81E-04	-1.92E-12	1.34E-13	1.81E-08	2.90E-03	1.47E-06	5.09E-06
SM	-20	-9.52E-11	-1.08E-11	1.88E-04	-1.21E-12	2.11E-13	1.21E-08	1.94E-03	9.77E-07	3.39E-06
SM	0	-4.76E-11	-5.42E-12	9.38E-05	-6.18E-13	8.43E-14	6.04E-09	9.68E-04	4.89E-07	1.70E-06
SM	40	4.76E-11	5.42E-12	-9.38E-05	6.18E-13	-8.43E-14	-6.04E-09	-9.68E-04	4.89E-07	1.70E-06
SM	60	9.52E-11	1.08E-11	-1.88E-04	1.21E-12	-2.11E-13	-1.21E-08	-1.94E-03	9.77E-07	3.39E-06
FM	-40	-2.07E-10	-1.05E-10	2.51E-04	7.52E-12	-6.92E-12	1.45E-07	1.23E-03	1.14E-07	6.14E-07
FM	-20	-1.38E-10	-7.00E-11	1.67E-04	4.98E-12	-4.58E-12	9.66E-08	8.19E-04	7.62E-08	4.10E-07
FM	0	-6.93E-11	-3.51E-11	8.37E-05	2.47E-12	-2.35E-12	4.83E-08	4.09E-04	3.81E-08	2.05E-07
FM	40	6.93E-11	3.51E-11	-8.37E-05	-2.47E-12	2.35E-12	-4.83E-08	-4.09E-04	3.81E-08	2.05E-07
FM	60	1.38E-10	7.00E-11	-1.67E-04	-4.98E-12	4.58E-12	-9.66E-08	-8.19E-04	7.62E-08	4.10E-07

Fig.4 shows the wavefront error plots for each mirror and the system image plane after macro file input fitting. Fig.4(a)-(c) presents the surface fitting results for the three mirror groups at -40°C, while Fig.4(d)-(f) shows the results at 60°C. These align closely with the rigid body displacement results from finite element analysis, indicating extremely

low fitting residuals and minimal computational error introduction. Fig.4(g) shows the wavefront error map of the system at -40°C, with an RMS of 0.1147 λ and a PV value of 0.3997 λ . Similarly, Fig.4(h) presents the wavefront error map at 60°C, exhibiting an RMS of 0.0752 λ and a PV value of 0.2668 λ . The system RMS is less than $\lambda/10$ at 60°C, while at

-40°C , the RMS is significantly worse than the 60°C condition by 30%. Similarly, the PV value at -40°C is worse than the 60°C condition by 30%. Results in both dimensions indicate significant changes in the primary mirror surface shape, with

greater disturbance to the optical system occurring under lower extreme temperatures. However, based on the RMS values, the system can be judged to function normally under extreme temperature conditions.

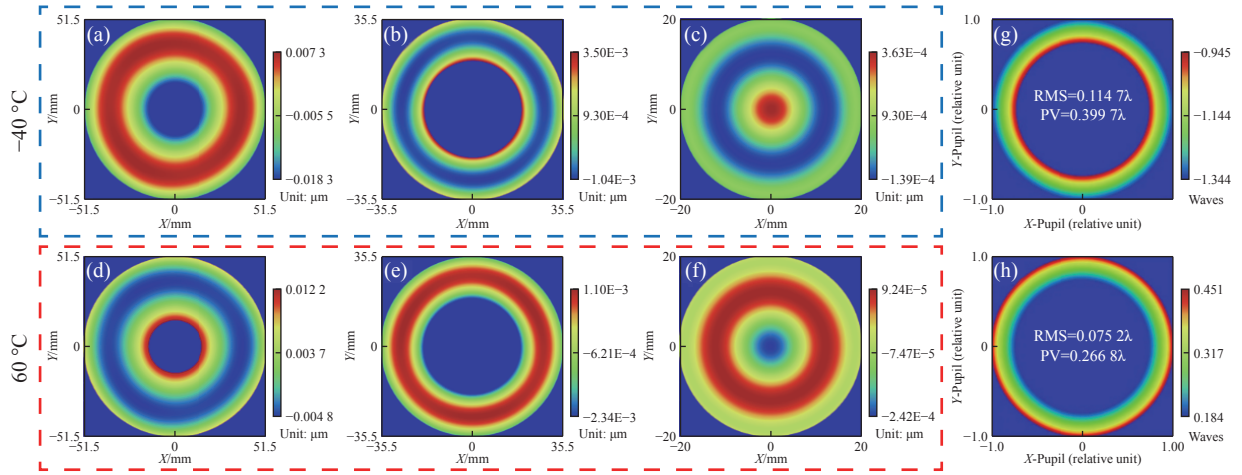


Fig. 4 (a) PM-TM surface morphology at -40°C . (b) PM-TM surface morphology at 60°C . (c) SM surface morphology at -40°C . (d) SM surface morphology at 60°C . (e) FM surface morphology at -40°C . (f) FM surface morphology at 60°C . (g) Wavefront error map of the system at -40°C . (h) Wavefront error map of the system at 60°C .

Fig.5 displays the Zernike coefficients of the primary mirror under both extreme temperatures. The distribution of Zernike polynomial coefficients indicates that the optical system's surface error is effectively controlled at both -40°C and 60°C extreme operating conditions. Overall, all Zernike terms remain at a low magnitude, with system errors primarily dominated by the first 15 low-order aberrations, which critically impact imaging quality. Comparison reveals that some coefficients at -40°C are slightly higher than at 60°C , confirming the analysis that low temperatures cause more significant optical surface perturbations. However, the overall magnitude change for all coefficients is limited. These results validate the system's thermally insensitive design, which combines glass ceramic mirrors with an Invar alloy support structure. Coupled with structural measures such as three-point flexible support, this approach successfully suppresses thermally induced surface distortion to an extremely low level across a wide temperature range. This provides critical assurance for maintaining ex-

cellent imaging performance in extreme temperature environments.

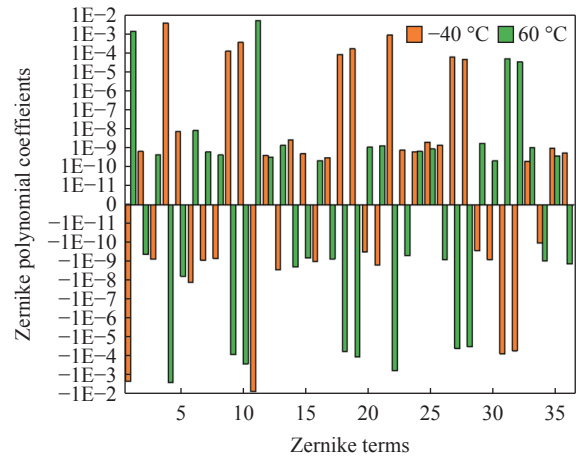


Fig. 5 Zernike polynomial coefficients of the primary mirror (PM) at extreme temperatures

Based on the aforementioned fitting and processing results, the rigid body displacement derived from structural-thermal-mechanical analysis and the Zernike polynomials were integrated into the optical design framework. This enabled a comprehensive evaluation of the star camera's optical performance under various operating conditions. The core per-

formance metrics assessed include the MTF and EE, both of which serve as fundamental quantitative standards for evaluating the efficacy of star camera optical systems in astronomical imaging. MTF primarily measures resolution and image sharpness, serving as a critical metric for most optical systems. EE assesses the proportion of total point source en-

ergy within a specified radius on the focal plane, holding significant importance for matching star camera lenses with detectors and for centroid positioning algorithms. The optical performance metrics under extreme operating conditions are shown in Fig. 6.

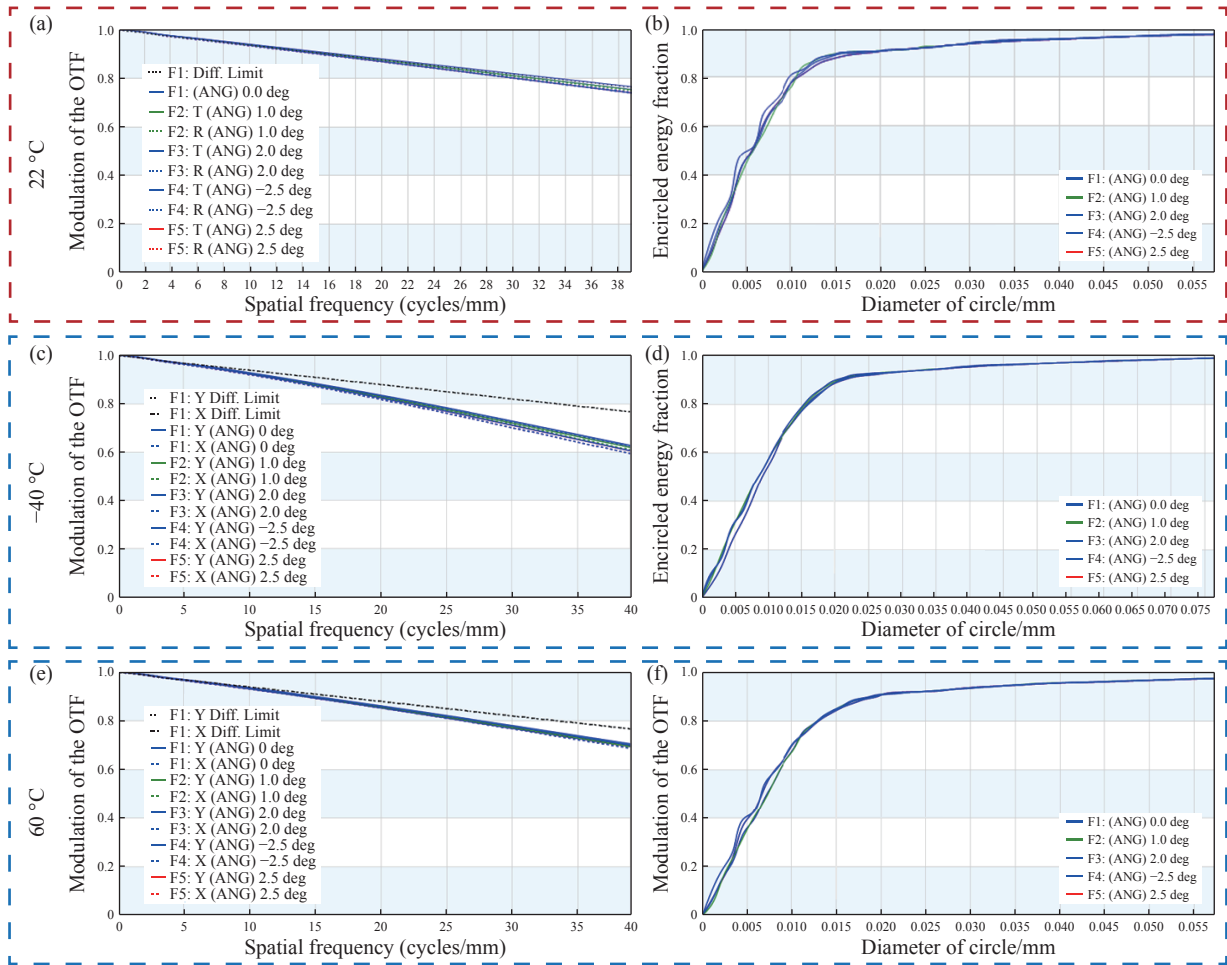


Fig. 6 (a) MTF at 22°C. (b) encircled energy at 22°C. (c) MTF at -40°C. (d) encircled energy at -40°C. (e) MTF at 60°C. (f) encircled energy at 60°C.

By incorporating optical-mechanical-thermal coupled analysis, we conducted a comprehensive evaluation of the overall performance of the star camera system. The results demonstrate the system's excellent performance under simulated extreme operating conditions, fully validating the robustness and engineering feasibility of the design. Specifically, under nominal conditions, the system's MTF approaches the diffraction limit at 40 line pairs per millimeter, with controllable degradation observed

under simulated conditions. The MTF decreased to 0.59 at -40°C low-temperature conditions, reflecting minor aberration amplification due to thermal contraction. The MTF value reached 0.68 at 60°C high-temperature conditions, indicating strong tolerance to thermal expansion. Compared to nominal conditions, these variations are controlled within 20%. MTF curves across all fields of view and both meridional and sagittal directions show no significant differences under these simulated conditions.

This validates uniform aberration distribution under thermal environments, preventing asymmetric degradation at field edges and ensuring consistent image quality across the entire field of view. Consequently, reliable celestial body tracking accuracy is maintained during actual space missions. The EE metric shows an 80% encircled-energy diameter of 10 μm under nominal conditions. At -40°C , this diameter increases to 15 μm , primarily due to slight energy dispersion caused by low-temperature-induced surface microdeformation. At 60°C , it measures 13 μm . Below is the signal-to-noise ratio calculation^[23] for observing a magnitude 10 star under extreme operating conditions at -40°C .

$$S_{\text{NR}} = \frac{S_e}{N}, \quad (6)$$

$$S_e = t \times \eta_{QE} \times k \times \tau \times E_{10} \times \pi \times \lambda \times \frac{D^2}{h \times c \times 4}, \quad (7)$$

$$k = \frac{a^2}{\frac{\pi d^2}{4}}, \quad (8)$$

$$N = \sqrt{S + \sigma_{\text{dark}}^2 + \sigma_r^2}, \quad (9)$$

In the equation, τ represents the optical system transmittance, E_{10} denotes the spatial irradiance at magnitude 10, typically $2.5 \times 10^{-10} \text{ W/m}^2$, D is the effective aperture of the optical system, t is the integration time, λ is the reference wavelength, here 650 nm, h is Planck's constant, c is the speed of light, η_{QE} is the quantum efficiency, calculated as 60% based on the detector's average efficiency, S_e is the number of electrons per pixel, the pixel size is $a \times a$, where $a = 6.5 \mu\text{m}$, the imaging spot diameter of the star camera is d , here 15 μm , S_{NR} is the signal-to-noise ratio, where S_e is the number of electrons used in the calculation, N is the total detector noise, σ_{dark} is the dark current noise, here 0.2 e⁻/pixel/s, σ_r is the readout noise, here 0.7 e⁻. Substituting all data yields an $S_{\text{NR}} > 12$ for observing a 10th magnitude star, confirming the system's capability to observe stars of magnitude 10.

This indicates the system maintains good focusing performance even at elevated temperatures. These variations align with design specifications and operational requirements, preventing significant SNR degradation caused by excessive energy dispersion. This ensures sensitivity in celestial body detection and accuracy in attitude determination.

4 Experimental validation of star camera performance

The system employs aspherical mirrors, whose imaging quality is highly dependent on the consistency between the actual surface shape and the ideal design. A laser interferometer was used to measure the surface shape of the three mirror groups. As shown in Fig.7(a)-(c), the well-defined and symmetric annular fringe patterns in the interferograms indicate excellent symmetry and effective suppression of low-order aberrations. The mirror surfaces are smooth and continuous, with minimal deviation from the ideal aspheric form. The RMS wavefront error of the primary mirror assembly is less than $1/40\lambda$. To measure the system-level wavefront error, a test platform was established using a 4D interferometer, as shown in Fig. 7(d). According to the Marechal criterion^[24], the total wavefront error RMS value of the system shown in Fig.7(e) is less than $\lambda/14$. This indicates that the MTF of the system is extremely close to the diffraction limit, enabling the formation of a highly concentrated star point image with sharp edges, which is crucial for resolving faint stars. The PV of 0.6201λ may lead to a slight increase in local scattered light, which could reduce image contrast and potentially introduce artifacts such as trailing or ghosting when observing bright stars.

Fig.8 shows the physical appearance of the coaxial quad-mirror camera system after precision alignment and assembly, along with its imaging performance test results under visible light. Fig.8(a) reveals the system's highly integrated opto-mechanic-

al structure featuring a compact axial layout with a total length of 50 mm. The external surfaces are treated with a black anodized finish to suppress stray light. Fig.8(b) demonstrates uniform illumination across the full image plane, with excellent imaging consistency between the edges and center of the field of view. The system exhibits outstanding image quality stability and is free from vignetting within a $\pm 2.5^\circ$ field. Figs. 8(c)-(d) show imaging results of ground targets at linear distances of 4000 m and 300 m respectively, revealing clear image details and sharp edges. These results further demonstrate the system's ability to maintain high-resolution imaging across varying object distances, with absolute distortion $\leq 0.1\%$. Star testing is essential for evaluating the optical performance and positioning accuracy of the star camera. By analyzing the imaging quality of point sources, the system's point spread function characteristics, centroid positioning accuracy, and thermal stability can be directly verified.

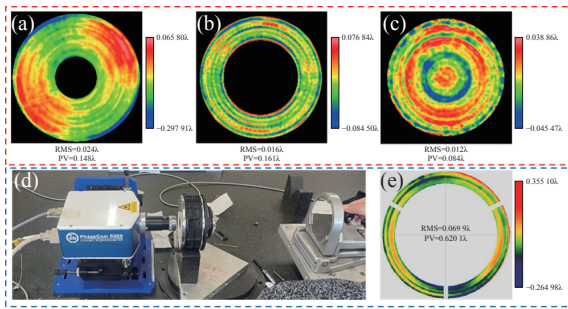


Fig. 7 (a) PM-TM topography results. (b) SM topography results. (c) FM topography results. (d) 4D interferometer test platform setup. (e) Systematic wavefront error results.

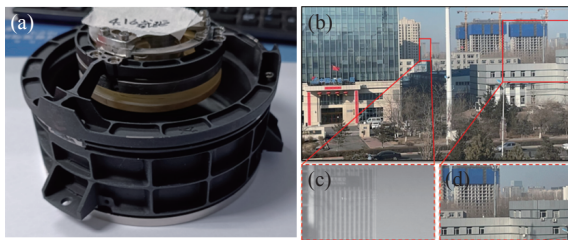


Fig. 8 (a) Physical image of the system. (b) Full-frame image of the system's image plane. (c) Physical image at a straight-line distance of 4000 m. (d) Physical image at a straight-line distance of 300 m.

Fig.9 presents the normalized field-of-view star point test results for the coaxial quad-reflector camera system under operating conditions of 22°C , -40°C , and 60°C , systematically assessing imaging stability and thermal insensitivity over a wide temperature range. After processing the full set of star point test data using a centroid algorithm, the centroid positioning repeatability was determined to be 0.08 pixels.

As shown in Fig.9(a), the star point exhibits high EE with a symmetrical and sharp outline at 22°C , approaching the diffraction limit. This indicates that the system delivers excellent image quality and positioning accuracy under nominal operating conditions. Fig.9(b) shows that at -40°C , although the star point exhibits slight diffusion, the energy distribution maintains good symmetry without noticeable trailing or splitting, demonstrating that the system remains controlled under low-temperature contraction stress. As shown in Fig.9(c), at 60°C , the star point morphology closely resembles that at room temperature, with only minor variations in EE, further validating the system's excellent tolerance to thermal expansion. These results align closely with conclusions from OMT integrated simulations incorporating Zernike surface fitting and wavefront error analysis. They confirm that the system maintains stable starlight imaging quality under extreme temperatures, satisfying the stringent thermal aberration suppression requirements for high-precision star sensors during in-orbit operation. The results also indicate that the -40°C condition imposes a more significant impact on camera performance, consistent with simulation predictions.

The repeatability of centroid positioning can assess the impact of temperature variations on angular resolution. The angular resolution calculation formula is as follows^[25]:

$$\rho = \frac{P}{f'} \times \frac{180}{\pi} \times 3600 \quad , \quad (10)$$

ρ represents the angular resolution of the system's pixels, P denotes the centroid positioning ac-

curacy of the pixels, and f' is the system's focal length. Based on comprehensive thermal vacuum testing, the centroid positioning repeatability was determined to be 0.08 pixels. Substituting this value

into the formula yields a maximum angular resolution error due to temperature variation of $0.55''$, meeting the sub-arcsecond precision requirement.

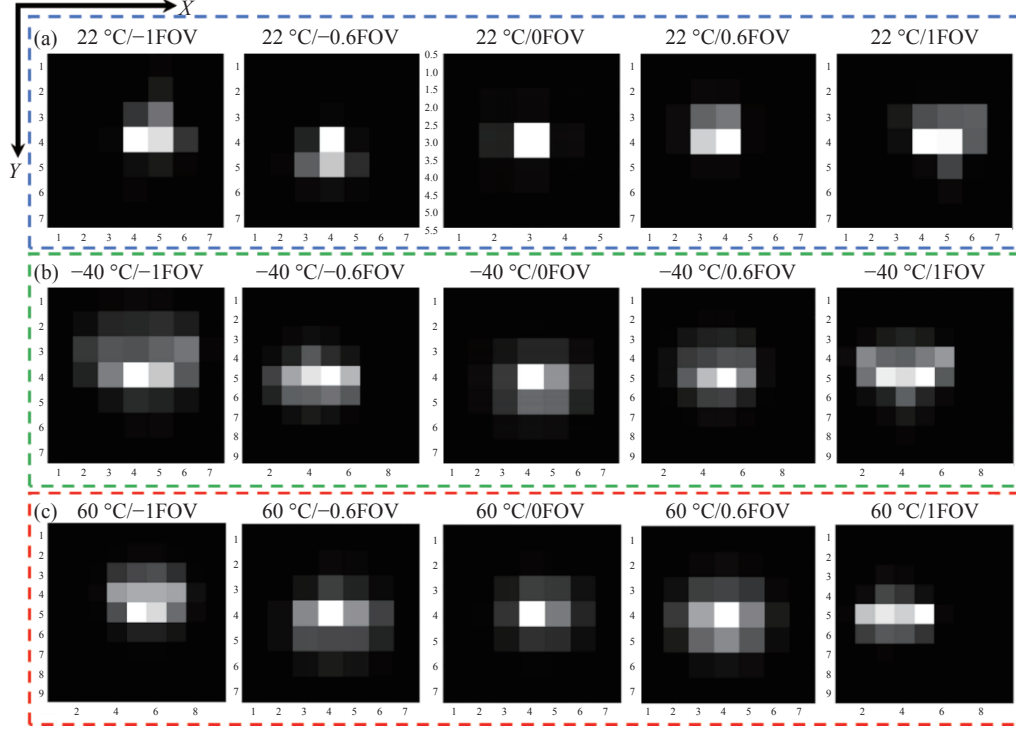


Fig. 9 Normalized field-of-view star point test results under different temperature conditions. (a) Test results at 22°C, (b) Test results at -40°C, (c) Test results at 60°C.

During space operations, the star tracker calculates the spacecraft's precise orientation in space by identifying stars against a star chart database. The star tracker's limiting magnitude detection capability directly determines its performance ceiling. A higher limiting magnitude allows for an exponential increase in the number of visible navigation stars, significantly enhancing the speed and reliability of star chart recognition. This provides more reference points with excellent spatial distribution for attitude determination, enabling sub-arcsecond-level high-precision results^[26]. Simultaneously, system robustness is significantly enhanced, enabling stable operation even when some stars are obscured or interfered with. The illuminance of a 6th-magnitude star and a 10th-magnitude star at the entrance pupil of the star camera is given by Equation (11).

$$\begin{aligned} E_6 &= 1.055 \times 10^{-8} Lx \\ E_{10} &= 2.649 \times 10^{-10} Lx \end{aligned} \quad (11)$$

Direct sunlight serves as the primary stray light source for astrophotographic cameras. The system's stray light suppression capability must be one order of magnitude lower than the image plane illuminance of 6th-magnitude and 10th-magnitude stars. The solar illuminance is denoted as $H = 1.3 \times 10^5 Lx$. The image plane illuminance of 6th-magnitude and 10th-magnitude stars is defined as in Equation (12):

$$\begin{aligned} H_6 &= E_6 \times \eta \times \frac{S}{S_1} \\ H_{10} &= E_{10} \times \eta \times \frac{S}{S_1} \end{aligned} \quad (12)$$

where η represents the system transmittance, which is 88% based on surface coating requirements. S denotes the effective entrance pupil area, S_1 indicates the starlight imaging area, stray light intensity $H_s = H \times PST(\theta)$, and $PST(\theta)$ signifies the point source transmittance at different fields of view within the system. Therefore, the PST magnitude for ob-

serving a 6th magnitude star must be less than 1.595×10^{-7} , and the PST magnitude for observing a 10th magnitude star must be less than 4×10^{-9} .

To achieve high detection capability while satisfying lightweight design constraints, as illustrated in Fig. 10, two distinct baffle configurations were developed for the star sensor optical system. The long baffle, depicted in Fig. 10(a), features inner and outer conical extinction vanes with a length of 480 mm and a mass of 725 g, whereas the short baffle in Fig. 10(b) incorporates inner conical extinction vanes with a length of 145 mm and a mass of 258 g. Both configurations utilize carbon fiber composites for structural integrity. The short baffle integrates isodensity conical vane rings on both internal and external surfaces, complemented by an external straight cylindrical profile. For enhanced stray light mitigation, the long baffle employs denser, computationally optimized non-equidistant vane rings distributed along its entire length. Employing PST methodology, the irradiance distribution at the entrance pupil was computed for each design. As presented in Fig. 10(c) and Fig. 10(d), both the long and short baffles yield an average irradiance of $1.08 \times 10^{-3} \text{ W/mm}^2$, aligning with established theoretical expectations.

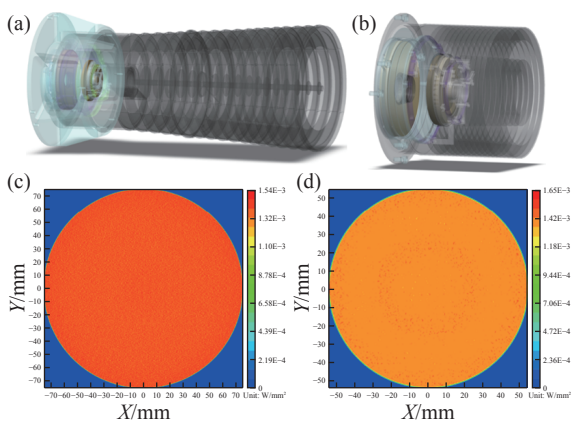


Fig. 10 (a) Long baffle structural design. (b) Short baffle structural design. (c) Irradiance distribution results at the entrance pupil for the long baffle. (d) Irradiance distribution results at the entrance pupil for the short baffle.

Fig.11 displays the stray light irradiance distribution for the long baffle at off-axis angles of

$30^\circ \sim 70^\circ$, clearly showing a decrease in stray light with increasing angle. Similarly, Fig.12 presents the stray light irradiance distribution for the short baffle at $40^\circ \sim 70^\circ$ off-axis angles, where stray light also progressively diminishes. As shown in Fig.12(h), the PST (LPST) of the long baffle is up to two orders of magnitude lower than that of the short baffle (SPST), enabling the observation of stars as faint as magnitude 10.

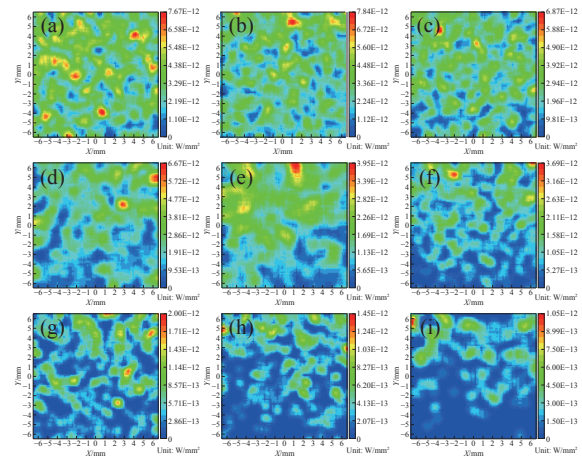


Fig. 11 Image plane stray light distribution of the long baffle at different off-axis angles. (a) 30° , (b) 35° , (c) 40° . (d) 45° . (e) 50° . (f) 55° . (g) 60° . (h) 65° . (i) 70° .

To thoroughly evaluate the engineering rationale for selecting the baffle design, this study conducted a systematic quantitative trade-off analysis between the long and short variants. While the short baffle aligns with the compact optical configuration of the star camera, the long baffle imposes significant resource costs, weighing approximately 2.8 times more and measuring about 3.3 times longer than its short counterpart. However, the long baffle achieves a generational leap in core performance, with a PST of 3×10^{-9} at 30° off-axis, which is nearly two orders of magnitude higher than the short baffle. This performance gap translates directly into a fundamental advantage in detection capability, enabling observations down to magnitude 10 stars. In contrast, the short baffle's stray light background limits its effective detection to approximately magnitude 6 stars. For high-precision attitude determination missions, the ability to utilize a large number of uniformly distributed faint stars directly determines

the system's positioning accuracy and robustness in complex illumination environments. Therefore, despite the long baffle adding approximately 467 g of mass and 335 mm to the envelope size, the performance margin gained through this resource cost is critical to ensuring mission success. Overall, prioritizing performance and reliability, the long baffle design represents the optimal balance between irreplaceable performance advantages and acceptable engineering trade-offs.

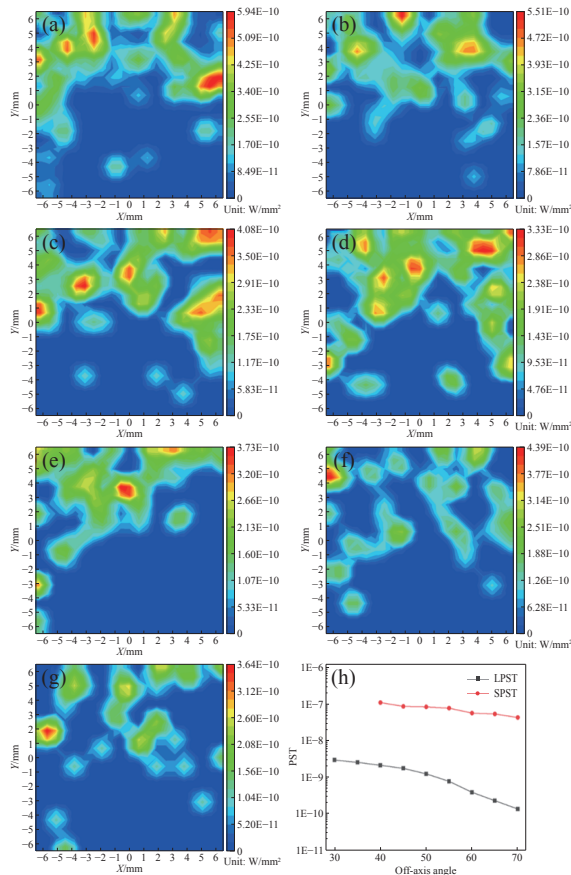


Fig. 12 Image plane stray light distribution of the short baffle at different off-axis angles and PST curves for both designs. (a) 40°. (b) 45°. (c) 50°. (d) 55°. (e) 60°. (f) 65°. (g) 70°. (h) Comparison curve between LPST and SPST.

5 Analysis of Results

Based on integrated opto-mechanical-thermal

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analysis and experimental validation of the star camera, the system demonstrates excellent thermal stability and stray light suppression capabilities under extreme temperature conditions ($-40^{\circ}\text{C}\sim+60^{\circ}\text{C}$). Through the coefficient of thermal expansion matching between the Zerodur® glass ceramic mirrors and the Invar alloy support structure, combined with a three-point flexible mounting system and a compact opto-mechanical architecture, thermally induced structural deformation and wavefront errors are effectively controlled. The wavefront error RMS value at 60°C is 0.0752λ , better than $\lambda/10$. The RMS value at -40°C is 0.1147λ , representing approximately a 30% degradation compared to the former, yet still meeting overall design requirements. Zernike polynomial fitting further indicates the system is dominated by low-order aberrations, with effective suppression of surface figure errors. The $\text{MTF}@40\text{ lp/mm}$ was 0.59 and 0.68 at -40°C and 60°C . The 80% encircled energy diameter was $15\text{ }\mu\text{m}$ at -40°C and $13\text{ }\mu\text{m}$ at 60°C , demonstrating excellent field-wide consistency. Regarding stray light analysis, the extended baffle design achieves LPST at the 10^{-9} level at 30° off-axis angle and LPST at the 10^{-11} level at 55° off-axis angle. This represents a two-order-of-magnitude improvement over the SPST achieved by the short baffle design, enabling detection of stars down to 10th magnitude. This coaxial quadruple-reflection camera system maintains optical performance suitable for high-precision centroid positioning across a wide temperature range through material matching, structural optimization, and baffle design. The methodology presented also provides a reference for the design, simulation, and validation of other cameras or payloads intended for faint targets under wide temperature variations.

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